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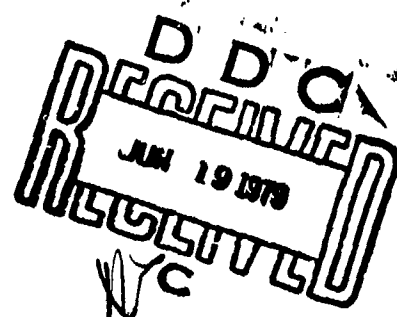
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FUEL TANK SURVIVABILITY FOR HYDRODYNAMIC RAM INDUCED BY  
HIGH-VELOCITY FRAGMENTS:

( Part I. Experimental Results and Design Summary

UNIVERSITY OF DAYTON RESEARCH INSTITUTE  
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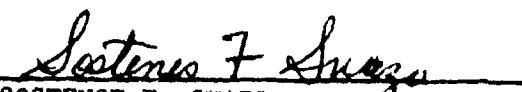
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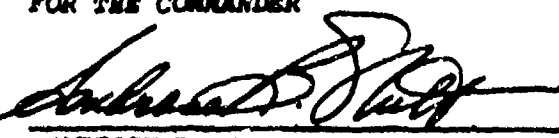
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Failure data, displacement data, and pressure data were obtained from laboratory experiments. Panels were made from 7075-T6 and 2024-T3 aluminum and from graphite epoxy; panel thicknesses were 1.6 to 6.35 mm. Protection included 10-mm-ballistic foam and stiffeners. Projectiles were 5.6-g and 11.7-g spheres and cubes.  Failures were always catastrophic, and failure thresholds were always abrupt. When cracks formed, they ran across the panels.			

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20. ABSTRACT (cont'd)

except when stiffeners were present. In thin panels, cracks initiated at the corners of the perforation when cubical fragments were used. The entrance panel damage was primarily induced by the shock wave generated by the impact. The very high shock pressure resulted in impulsive loading of the panels that caused prompt crack formation. Cracks were propagated by the displacement field. The dependence of failure on impact parameters could be approximately represented as  $V^2D/W = \text{constant}$ , where V is projectile velocity, D is projectile diameter, and W is panel thickness. Stiffeners and foam suppressed catastrophic crack formation.

Results were compared with finite difference (CRALE) and finite element (NONSAP) calculations. CRALE was able to calculate pressure caused by shock and drag forces on the projectile. A simplified loading model based on the details of the CRALE results was developed. When NONSAP was driven by the simplified loading model, neither displacement nor failure thresholds were correctly predicted. The error in displacement was attributed to neglect of late-time water resistance to panel slow down and rebound.

Design data that included failure criteria and pressure loading were generated for all configurations studied. The failure data were based on experimental results, but numerical results were used to aid interpolation and extrapolation. The pressure loading functions were based on an analytic fit to the numerical results. Failure of stiffened panels was defined by stiffener crushing.

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## PREFACE

The program described in this report was carried out for the Survivability/Vulnerability Branch of the Flight Vehicle Equipment Division (AFFDL/FES) of the Air Force Flight Dynamics Laboratory (AFFDL), Wright-Patterson AFB, Ohio. Most of the work was done under Contract F33615-77-C-2082. The Work Unit Title was "Aircraft Fuel Tank Wall Responses to Hydrodynamic Pressures", Number 24020218. Mr. William E. Hackenberger was the monitor for that contract. The experiments with graphite epoxy panels were mostly sponsored under Contract F33615-76-C-3076, Request 45. Mr. Andre Holten, AFFDL, was the Technical Monitor for that effort.

Many members of the staff of the University of Dayton Research Institute contributed to this program. Dr. Stephan Bless served as Principal Investigator. The experiments were executed under the supervision of Mr. Michael Nagy. Mr. Andrew Piekutowski was responsible for the improved moiré fringe set-up and analysis. Dr. John Barber was responsible for technical oversight, and Mr. Hallock Swift was Project Supervisor.

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## SYMBOLS AND UNITS

The primary units used in this report are a combination of cgs and SI units that is commonly used in impact physics. The unit of pressure in the bar, defined as  $10^6$  dyne/cm<sup>2</sup>. One bar is very nearly one atmosphere, so that pressure, expressed in bars, is numerically equal to the ratio of actual pressure to initial pressure. A list of conversions to SI and English units appear below.

<u>Quantity</u>	<u>This Report</u>	<u>SI</u>	<u>English</u>
velocity	km/s (mm/ s)	1 km/s	3281 ft/s
stress (pressure)	bars (kbars)	0.1 MN/m <sup>2</sup>	14.5 psi
length	mm (cm, m)	1 mm	0.039 in.
mass	g	10 <sup>-3</sup> kg	15.43 grains
density	g/cm <sup>3</sup>	1000 kg/m <sup>3</sup>	62.4 lb/ft <sup>3</sup>
area impulse	bar-s	10 <sup>5</sup> kg/ms	14.5 psi-s

Shot configurations are referred to by abbreviations in this report. The key to this nomenclature is given in Table III of the text.

The following symbols are used in equations:

$a$	acceleration
$A$	presented area of the projectile
$A_h$	area of the impact hole
$A_H$	Hugoniot pressure increment per increment in impact velocity
$C$	shock speed in water
$C_p$	shock speed in projectile
$d$	diameter of the impact hole
$D$	diameter of a spherical projectile, edge dimension
$F$	failure constant of a cubical projectile
$I$	impulse
$L_p$	panel width
$P$	pressure
$P_H$	Hugoniot pressure
$P_m$	maximum pressure
$r$	radial coordinate in a cylindrical coordinate system centered on the impact
$R_p$	radius of a spherical projectile of mass equal to that of the actual projectile
$t$	time, shock
$u$	particle velocity
$u_H$	peak shock particle velocity from 1-D impact
$u_z$	displacement displacement in z direction
$V$	impact velocity
$V_F$	velocity at which panel failure occurs
$W$	panel thickness
$z$	axial coordinate in a cylindrical coordinate system centered on the impact, positive into the tank
$\delta$	small displacement
$\epsilon$	strain
$\epsilon_\theta$	hoop strain
$\rho$	projectile density

$\rho_t$  target panel density  
 $\sigma_r$  radial stress  
 $\sigma_\theta$  hoop stress

## SECTION I

### INTRODUCTION

The design of fuel tanks for combat aircraft must take into account hydrodynamic ram phenomena. Fuel system hits are the largest single cause of aircraft loss in combat, and hydrodynamic ram is the primary damage mechanism when liquid-filled structures are penetrated by projectiles.

Hydrodynamic ram phenomena are varied and depend on threat and target parameters. In particular, there are three essentially different classes of threats: gun-launched KE projectiles, gun-launched HE projectiles, and high-velocity fragments. The effects of these projectiles are different for each of the three types of target subjects: entrance panels, side panels, and exit panels. In general, relatively slow and massive gun launched KE projectiles pose the greatest threat to exit panels. HE projectiles, depending on where they detonate, may impose severe loads on any panel. On the otherhand, high-velocity fragments principally damage entrance panels; exit panel damage only occurs when a relatively large number of fragments strike a relatively shallow tank.

Hydrodynamic ram phenomena have been subjected to vigorous study in recent years. A summary of most results through 1976 can be found in Reference 1.

Herein we report experimental and analytical results concerning damage of entrance panels by high-velocity fragments. The principal objectives of the program were to develop a design methodology and prepare design guidelines for fuel tank entrance panels. The threats considered were cubes and spheres ranging in mass from 5.6 g to 11.6 g (90 to 180 grains). Target panels were representative of present and planned designs, viz. plain, foam-backed, and stiffened 2024-T3 and 7075-T6 aluminum.



Panel thickness varied from 1.6 mm (1/16-inch) to 6.35 mm (1/4-inch.)

The technical approach consisted of carefully coordinated numerical analyses and experiments. In the analyses, a finite difference code was used to calculate the pressure at the liquid-panel interface. The derived loading functions were then used to drive a finite element model of the entrance panel. The result has been a greatly improved physical understanding of the panel damage mechanism and predictions of panel failure and displacement.

The experiments were designed to calibrate and validate the numerical models, as well as to provide "hard" data on which to base design curves. The targets were instrumented in order to obtain pressure and displacement data for comparison with code calculations.

The University of Dayton was responsible for program management, conduct of experiments, interpretation of experimental and numerical results, and generation of design data. California Research and Technology, Inc. (CRT) was responsible for the numerical modelling.

Part I of this report was prepared by the University. Section II of Part I describes the experiments from a functional point of view, and Appendix A and Reference 2 present a further description of the laboratory and data analysis techniques. Section III summarizes and discusses the results of the experiments. Section IV summarizes the analytical results and compares them with the experiments. Section V presents a synthesis of the experimental and numerical work in order to produce design guidelines. Section VI presents a summary of results and conclusions. A series of graphs is included to aid designers of damage-tolerant fuel tanks.

Part II of this report describes the numerical approaches in detail. In Part II, the numerical data are analyzed and compared with the experiments in order to obtain an improved physical understanding of hydrodynamic ram phenomena.

## SECTION II

### EXPERIMENTAL CONFIGURATION

The experiments consisted of single impacts of gun-launched projectiles into water-backed panels. Most shots were well instrumented; data were obtained for pressure behind the panels, dynamic displacement, and failure modes. The projectiles were cubes and spheres. Details of the experimental apparatus and data reduction techniques are given in Appendix A. Below we describe the features of the experiments most relevant to the interpretation of results.

#### 2.1 BALLISTICS

The dimensions of the projectiles are given in Table I. The tolerance on the mass was  $\pm 0.05$  g. Sabots were serrated Lexan<sup>®</sup>. Flash radiographs were used to measure velocity and to verify the integrity and orientation of the projectiles prior to impact. Rotation of the cubical projectiles during free flight was minimal.

#### 2.2 TARGET CONSTRUCTION

The targets were panels fastened over a cut out in the front of a  $0.8 \text{ m}^3$  water-filled tank. Figure 2.1 provides a drawing of the structure of the tank. One side of the tank and the rear panel were 1.6-mm-thick aluminum, and the other side was 12.7-mm aluminum. The top was open, and the bottom rested on a 12.7-mm plywood sheet. The front of the tank, minus the target panel, is shown in Figure 2.2. The mounting plate was 12.7-mm-thick aluminum. The edge dimension of the cut out was 51 cm. The mounting holes were  $3/8$  inch, and the distance between bolt centers was 63.5 cm.

In all of the shots prefaced by FD, except those against the graphite epoxy panels, the target panels were aluminum sheets bolted directly to the mounting plate. The edge

TABLE 1. PROJECTILE DESCRIPTIONS

<u>Large Projectiles</u>	Weight <sup>*</sup> (g)	Edge or Diameter (mm)
Cube	11.67	11.41
Sphere	11.89	14.25
<u>Small Projectiles</u>		
Cube	5.66	8.97
Sphere	5.59	11.08

\*11.67 g = 180 grains  
 11.89 g = 183 grains  
 5.66 g = 87 grains  
 5.59 g = 86 grains

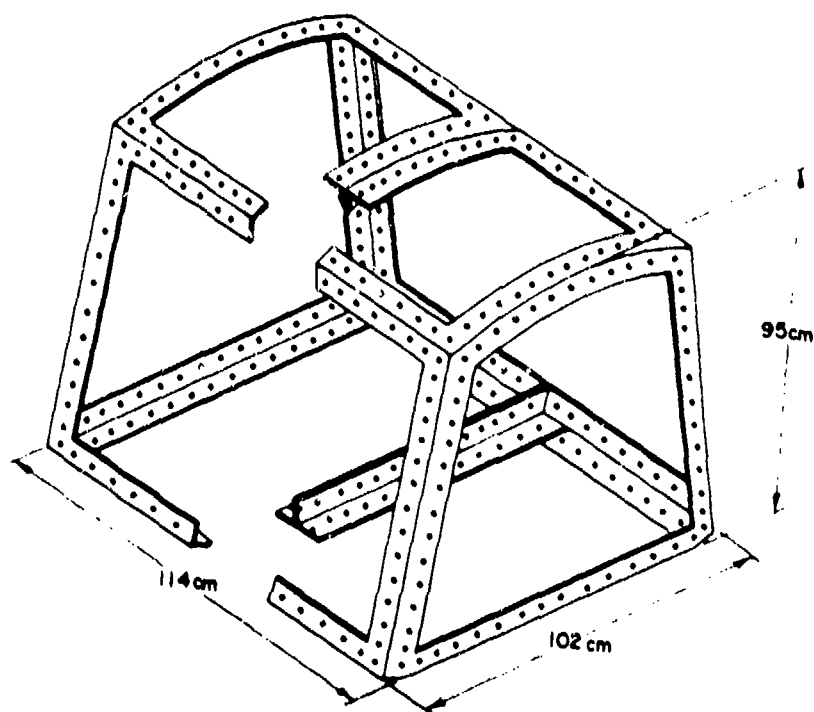


Figure 2.1. Frame of target tank.

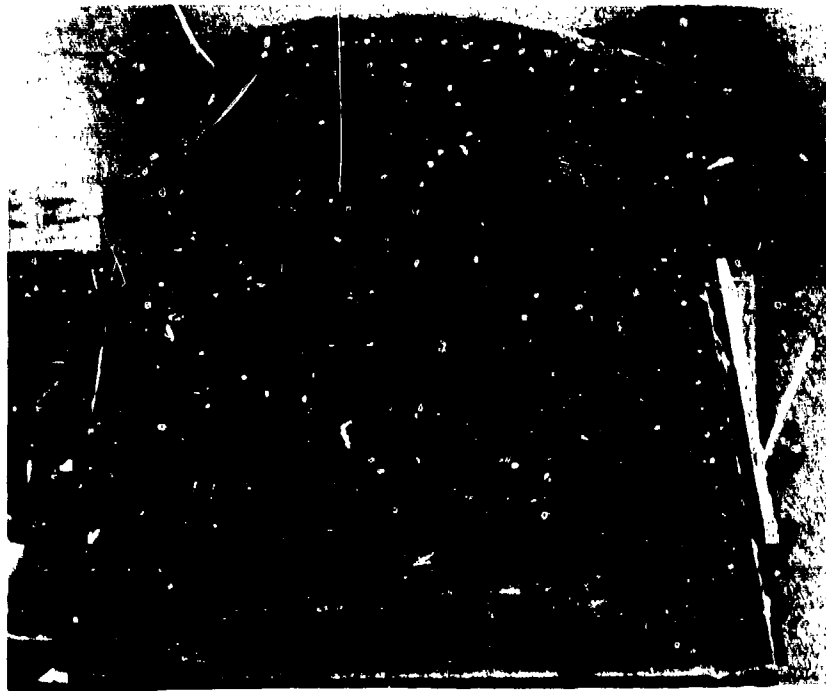


Figure 2.2. Photograph of front of target tank without the target panel.

dimension of these target panels was 69 cm. In shots labelled FTC and in the FD graphite epoxy shots, the panels were rigidly clamped on the edges.

Some of the aluminum panels were reinforced with stiffeners. Figure 2.3 shows a drawing of the stiffener geometry, and Figure 2.4 shows a panel with the stiffeners applied. The panels were mounted 13.8 cm apart with 4.8-mm-diameter, solid round-head aluminum rivets on 25.4 mm centers. The junction between the stiffener edge and the panel was caulked with RTV. End caps were placed on the stiffeners to keep water out. Water seepage into the stiffeners was also alleviated by drilling drainage holes on the bottom of the front of the panel (except in Shots 34 and 35).

Many panels were protected by application of 10 mm of closed-cell foam (Avco Thermaest AX5052). The adhesive conformed to MIL-S-88020 and 2, class B-2. It was manufactured by Products Research and Chemical Corporation, Item PR-1422 B-2. It was applied with a 1/8-inch trowel. Foam was only applied to the aluminum basic plate; when stiffeners were present the foam did not cover the rivets on the stiffener skin.

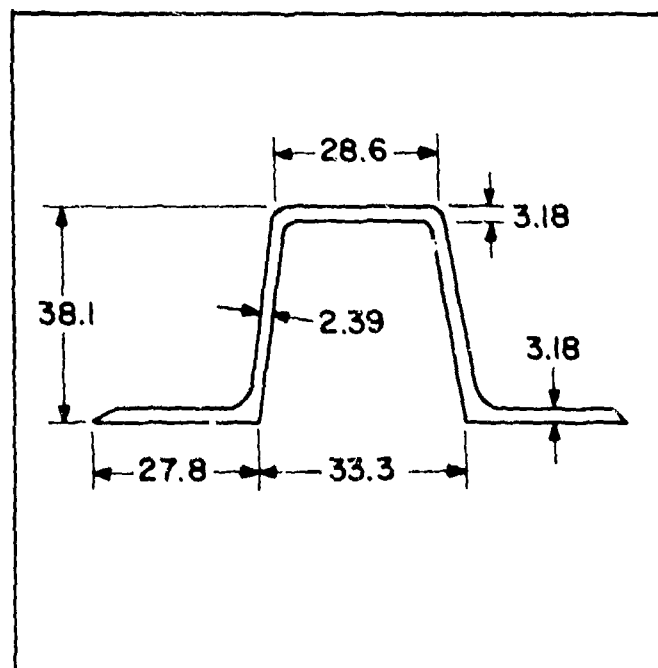


Figure 2.3. Stiffener geometry. Material is 6063-T6 aluminum. Dimensions are in mm.



Figure 2.4. Illustration of construction of stiffened panel.

The graphite epoxy panels were supplied by AFFDL, and their density was determined to be  $1.49 \text{ g/cm}^3$ . The following description was supplied by Mr. R. Achard of AFFDL/FBS:

The graphite panels were fabricated as 4 ft. x 6 ft. plates, 24 plies and 32 plies, respectively, in two autoclave runs. The nominal lay-up for the panels was  $[0/\pm 45/90]_{3S}$  for the 24 ply panel and  $[0/\pm 45/90]_{4S}$  for the 32 ply panel. These panels were fabricated from Hercules 3501/AS material using the standard curing cycle which was as follows: The material was set up on a flat plate with bleeder on both sides, the bleeder being separated from the graphite/epoxy prepreg by separator plies of Teflon coated glass. The assemblage of bleeder, separators and uncured graphite epoxy was enclosed in a vacuum bag and subsequently full vacuum of 28 inches mercury was applied. The panel was heated in the autoclave to  $225^\circ\text{F}$  in approximately 70 minutes under full vacuum. At  $225^\circ\text{F}$ , 85 psi autoclave pressure was applied, and the full vacuum maintained for one hour. After the one hour hold at  $225^\circ\text{F}$ , the vacuum bag was vented to atmosphere and the part heated to  $350^\circ\text{F}$  in approximately 50 minutes. At  $350^\circ\text{F}$  the part was maintained for one hour. This completed the cure. Subsequently, the part was cooled to below  $150^\circ\text{F}$  under pressure and then removed from the autoclave. Both panels were dimensionally checked and found to be warp free and to have a per ply thickness of .0055 for the 24 ply laminate and .0054 for the 32 ply laminate.

The cured 4 ft. x 6 ft. panels were subsequently cut into 2 ft. x 2 ft. squares using a band saw with a silicone carbide blade. Fiberglass frame tabs (.16 in thick x 2 in wide) were then bonded to the panels using room temperature curing two-part epoxy cement. The panels were subsequently delivered for test.

### 2.3 INSTRUMENTATION

Three types of terminal effects measurements were made for most shots. Pressure at several points within the fluid was measured with small tourmaline transducers. These transducers are described in Appendix A; they possessed a frequency response of almost 1 MHz. The locations of the transducers are given in Table II.

TABLE II. TRANSDUCER LOCATIONS

<u>Identity</u>	<u>r</u> <u>(mm)</u>	<u>z</u> <u>(mm)</u>
T1	75	6
T2	100	6
T3	200	6
T4	100	100
T5	100	200

Dynamic panel displacement was determined with a moiré fringe apparatus as described in Reference 2. A description of the revised system and the analysis techniques is also provided in Appendix A. The moiré fringe system possessed a maximum resolution of 0.2 mm, and it viewed a little more than half the target panel.

### 2.4 NOMENCLATURE

Throughout this report we use a set of abbreviations in order to easily describe specific shot configurations. The

abbreviations are defined in Table III. The first symbol indicates the panel material and thickness. The second symbol indicates the projectile shape and size. The third symbol indicates the presence or absence of foam protection. Impact velocity is indicated by its value, in km/s written after the symbols. For example, 7L1.60 represents a 6.35-mm-thick 7075-T6 panel struck by a 11.7-g cube at 1.60 km/s.

TABLE III. NOMENCLATURE

First Symbol

- 7 = 1.6-mm-thick 7075-T6 panel
- 2 = 1.6-mm-thick 2024-T3 panel
- 7 = 6.35-mm-thick 7075-T6 panel
- 2 = 6.35-mm-thick 2024-T3 panel
- 2<sup>^</sup> = 1.6-mm-thick 2024-T3 panel with "hat" stiffeners
- G = 3.18-mm-thick graphite epoxy panel
- G = 4.8-mm-thick graphite epoxy panel

Second Symbol

- L = 11.7-g cube projectile
- S = 5.7-g cube projectile
- L' = 11.7-g spherical projectile
- S' = 5.6-g spherical projectile

Third Symbol

- F = 10-mm ballistic foam
- Blank = no foam



### SECTION III

#### EXPERIMENTAL RESULTS

Fifty-seven shots were fired during this program. Nine of these did not yield data for various reasons. The configurations tested, the instrumentation used, and the primary results are listed in Table IV. The first columns of the table give the shot number and impact configuration (see Table III for an explanation of the abbreviation code.) The next five columns explicitly describe the shot configuration. The next column reports the diagnostics, and the last column gives the results. In all but the stiffened panel, "Failed" refers to catastrophic crack formation. The shots designated FD were performed under Contract F33615-77-C-2082, and those designated FTC were under Contract F33615-76-C-3076. The types of data obtained were failure modes, fluid pressures, and panel displacements. The observations are discussed in detail in the following paragraphs.

The cube projectiles fired against thick panels were frequently recovered as many fragments. The threshold for fragmentation of the larger projectiles (11.7 g) was between 1.42 km/s and 1.47 km/s. Comparison of data from pairs of shots that bracketed a panel failure threshold showed a slight tendency for reduced damage to the projectile just above the failure threshold. Foam had little effect on projectile fragmentation. Projectiles fired against thin aluminum targets (1.6 mm) did not fragment.

#### 3.1 FAILURE

The primary results of this program are failure data. Other types of data serve as diagnostics to aid in the understanding, interpolation, and extrapolation of the failure data. The scope of impact parameters that were to be examined was

TABLE IV. DATA SUMMARY

Shot No.	Configuration	Projectile Mass (g)	Target Material	Target Thickness (mm)	Foam Thickness (mm)	Impact Velocity (km/s)	Diagnosis	Results
FD1	7L	11.66	7075-T6	6.35	0	1.82	D	Failed
FD2	7L	11.66	7075-T6	6.35	0	1.42	D	OK
FD3	7L	11.66	7075-T6	6.35	0	1.42	P,D	OK
FD4	7L	11.66	7075-T6	6.35	0	1.47	P,D	OK
FD5	7L	11.66	7075-T6	6.35	0	1.60	P,D	OK
FD6	7L	11.66	7075-T6	6.35	0	1.69	P	Failed
FD7	7L	11.66	7075-T6	6.35	0	1.59	D	OK
FD8	7L	11.66	7075-T6	6.35	0	1.63	P,D	OK
FD9	7LF	11.66	7075-T6	6.35	10	-----	----	Sabot failure
FD10	7LF	11.66	7075-T6	6.35	10	-----	----	Sabot failure
FD11	7LF	11.66	7075-T6	6.35	10	1.78	P,D	OK
FD12	7LF	11.66	7075-T6	1.6	10	1.75	P,D	Failed
FD13	7LF	11.66	7075-T6	1.6	10	1.58	P,D	OK
FD14	7LF	11.66	7075-T6	1.6	10	1.67	P,D	Failed
FD15	7S	5.66	7075-T6	1.6	0	1.98	P,D	Failed
FD16	7SF	5.66	7075-T6	1.6	10	1.99	P,D	OK
FD17	2L	11.66	2024-T3	6.35	0	-----	----	sabot failure
FD18	2L	11.66	2024-T3	6.35	0	-----	----	Sabot failure
FD19	7S	5.66	7075-T6	6.35	0	1.95	P,D	OK
FD20	7S	5.66	7075-T6	1.6	0	-----	----	Incomplete powder burn
FD21	7S	5.66	7075-T6	1.6	0	1.82	P,D	Failed

TABLE IV. DATA SUMMARY (CONT'D)

Shot No.	Configu- ration	Projectile Mass (g)	Target Material	Target Thickness (mm)	Foam Thickness (mm)	Impact Velocity (km/s)	Diag- nostics	Results
FD22	7S	5.66	7075-T6	1.6	0	1.74	P,D	Failed
FD23	7S	5.66	7075-T6	1.6	0	1.35	P,D	Failed
FD24	7S	5.66	7075-T6	1.5	0	-----	-----	Sabot failure
FD25	GS*	5.59*	gr-ep	4.76	0	1.46	P,D	Failed
FD26	GSF*	5.59*	gr-ep	3.18	10	1.69	P,D	OK
FD27	7S	5.66	7075-T6	1.6	0	1.24	-----	Failed
FD28	7S	5.66	7075-T6	1.6	0	0.92	-----	OK
FD29	7S	5.66	7075-T6	1.6	0	1.06	-----	Failed
FD30	2L	11.66	2024-T3	6.35	0	1.62	P	OK
FD31	2L	11.66	2024-T3	1.6	0	1.32	P	Failed
FD32	2LF	11.66	2024-T3	1.6	10	-----	-----	Sabot failure
FD33	2L	11.66	2024-T3	1.6	0	1.30	-----	Failed
FD34	2L	11.66	2024-T3*	1.6	0	1.49	-----	Failed <sup>†</sup>
FD35	2L	11.66	2024-T3*	1.6	0	1.20	-----	Failed <sup>†</sup>
FD36	2L	11.66	2024-T3*	1.6	0	-----	-----	Sabot failure
FD37	2LF	11.66	2024-T3*	1.6	10	1.56	P	Failed <sup>†</sup>
FD38	2S	5.66	2024-T3*	1.6	0	1.19	P	OK
FD39	2LF	11.66	2024-T3*	1.6	10	1.35	P	Failed <sup>†</sup>

\*These panels had stiffeners.

†Failure for stiffened panels was defined as more than 5 rivets popped and 15 mm permanent displacement on any one stiffener.

TABLE IV. DATA SUMMARY (CONT'D)

Shot No.	Projectile Configuration	Projectile Mass (g)	Target Material	Target Thickness (mm)	Foam Thickness (mm)	Impact Velocity (km/s)	Diagnosis	Results
FD40	2S	5.66	2024-T3*	1.6	0	1.33	P	OK
FD41	2S	5.66	2024-T3*	1.6	0	1.35†	P,D,E	Failed †
FD42	2L	11.66	2024-T3*	1.6	0	0.08	-----	No perf.
FD43	2L	11.66	2024-T3*	1.6	0	1.12	P	OK
FD44	2LF	11.66	2024-T3	1.6	10	1.68	-----	OK
FTC1	7S	5.59*	7075-T6	3.18	0	1.55‡	D	Failed
FTC2	2S	5.59*	2024-T3	3.18	0	1.55‡	D	OK
FTC3	GS	5.59*	gr-ep	3.18	0	1.51‡	P	Failed
FTC4	GS	5.59*	gr-ep	3.18	0	0.96	D	OK
FTC5	GS	5.59*	gr-ep	3.18	0	1.28	D	Failed
FTC6	GS	5.59*	gr-ep	3.18	0	1.01	D	OK
FTC7	GS	5.59*	gr-ep	3.18	0	1.18	D	Failed
FTC8	GS	5.59*	gr-ep	4.76	0	1.11	D	OK
FTC9	GS	5.59*	gr-ep	4.76	0	1.36	D	OK
FTC10	GS	5.59*	gr-ep	4.76	11	1.63	D	Failed
FTC11	GSF	5.59*	gr-ep	4.76	11	1.95	D	OK
FTC12	GSF	5.59*	gr-ep	4.76	11	1.98	D	OK
FTC13	GSF	5.59*	gr-ep	3.18	11	1.18	D	OK

\*Spherical projectile

†These panels had stiffeners. Failure for stiffened panels was defined as more than 5 rivets popped and 15-mm permanent displacement on any one stiffener.

‡No x-ray obtained. Uncertainty in velocity is ±8%, -1%.

§Same panel as FD42.

5.6-g to 11.2-g (90 to 180 grain) mass fragments at velocities between 1.4 km/s and 2.0 km/s. The lower velocity limit was later extended in order to observe critical failure phenomena in thin target panels. The failure data obtained for each configuration are given in Table V. For each configuration, this table lists the relevant shots, the experimental limits, or the failure threshold velocity.

### 3.1.1 Modes of Failure

In most instances, failure was characterized by a distinct threshold velocity. Below the threshold, the only damage to the target panel was the primary perforation and some permanent plastic deformation. Above the threshold, cracks formed that ran to the edges of the panel.

In the plain aluminum panels, failure occurred as relatively straight cracks. Crack patterns were similar in 2024-T3 and 7075-T6 panels. In thin (1.6 mm) panels struck by cubes, the cracks started at the corners of the square perforation hole in the panel. There were two cracks at velocities just below the threshold. As velocities increased, three, then four cracks formed. The orientation of the crack system was the same as that of the thin panels struck by cubical projectiles. These observations are illustrated in Figures 3.1, 3.2, and 3.3, which show aluminum panels struck at velocities below the failure threshold, a little above the threshold, and well above the threshold. The cracks in these panels ran straighter than those reported previously in similar panels failed by spherical projectiles<sup>2</sup>.

In thick panels (6.35 mm), no more than two cracks were observed. For example, Figures 3.4 and 3.5 illustrate the failure in a 6.35-mm-thick 7075-T6 panel struck by a 11.7-g projectile at 1.82 km/s (configuration 7L1.82).

Foam-backed aluminum also failed catastrophically. When failure occurred, the cracks were not straight as in plain panels. Figure 3.6 illustrates the "S" pattern formed by the cracks in a foam-backed 7075-T6 panel.

TABLE V. SUMMARY OF FAILURE DATA

<u>Configuration</u>	<u>Shots</u>	<u>Results</u>
<u>7L</u>	FD1-8	$V_F = 1.66 \pm .03$
<u>7LF</u>	FD11	$V_F > 1.95$
<u>7S</u>	FD19	$V_F > 1.95$
<u>7SF</u>	----	Est. $V_F > 2.0$
<u>7L</u>	----	Est. $V_F < 0.99$
<u>7LF</u>	FD12, 13, 14	$V_F = 1.62 \pm .05$
<u>7S</u>	FD15, 21, 22, 23, 27, 28, 29, FTC1	$V_F = 0.99 \pm .06$
<u>7SF</u>	FD16	$V_F > 1.99$
<u>2L</u>	FD30	$V_F > 1.62$
<u>2LF</u>	----	Est. $V_F > 2.0$
<u>2S</u>	----	Est. $V_F > 2.0$
<u>2SF</u>	----	Est. $V_F > 2.0$
<u>2L</u>	FD31, 33, FT6, FTA7, 9	$V_F < 1.30$
<u>2LF</u>	FD44, FTA11	$V_F > 1.68$
<u>2S</u>	FT5, 5A, 5B; FTC2 FTA3, 4, 5, 6, 10	$V_F = 2.08 \pm .13$
<u>2SF</u>	FTA12	$V_F > 2.38$
<u>GS</u>	FTC8, 9, 10 FD25	$V_F = 1.47 \pm .05$
<u>GSF</u>	FTC10, 11	$V_F > 1.36$
<u>GS</u>	FTC4, 5, 6, 7	$V_F = 1.09 \pm .08$
<u>GSF</u>	FTC13, FD26	$V_F > 1.69$
<u>2L</u>	FD34, 35, 36, 43	$V_F = 1.16 \pm .04$
<u>2S</u>	FD38, 40, 41	$V_F = 1.34 - .01, +.11$
<u>2LF</u>	FD37, 39	$V_F < 1.35$
<u>2LE</u>	----	Est. $V_F = 1.34$

## NOTES:

FT and FTA shots were reported in AFML-TR-77-11.  
 FTC shots were done under Contract F33615-76-3076  
 Request 45.  
 FT, FTC, and FD25 and 26 used spherical projectiles;  
 other shots used cubes.



Figure 3.1. Panel from Shot FD28 (7S0.92), showing damage at just below the failure threshold in thin 7075-T6 panels.

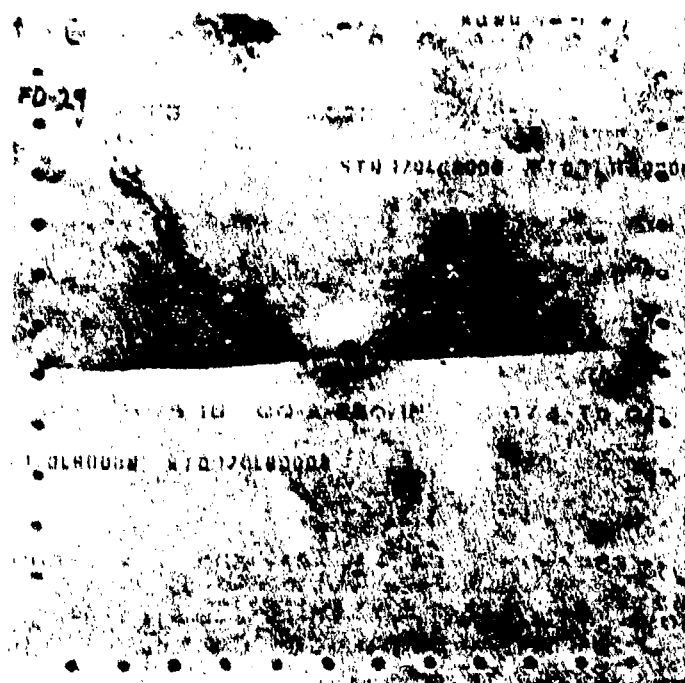


Figure 3.2. Panel from Shot FD29 (7S1.06), same as Fig. 3.1 except a little above the failure threshold.

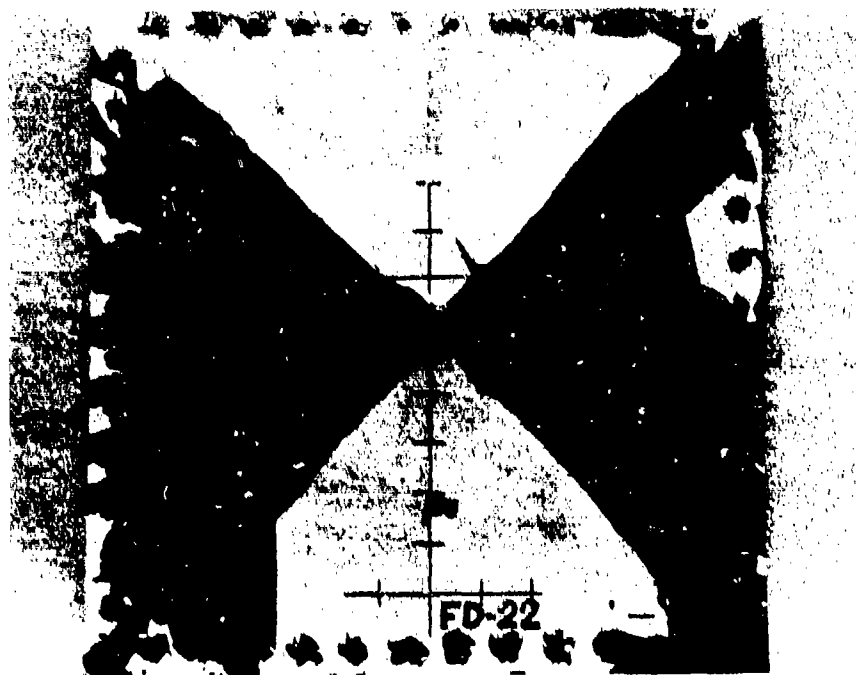


Figure 3.3. Panel from Shot FD22 (7S1.74); like Fig. 3.2 but well above the threshold velocity.

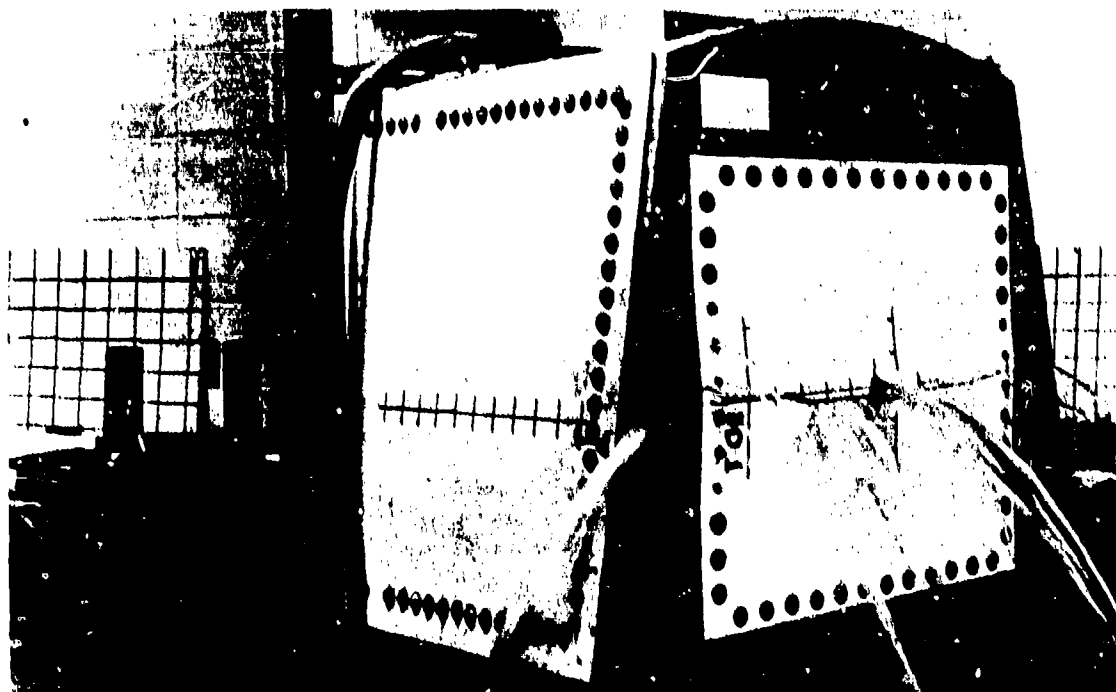


Figure 3.4. Panel from Shot FD1 (7L1.82), showing crack pattern in thick 7075-T6 panel struck well above failure threshold.



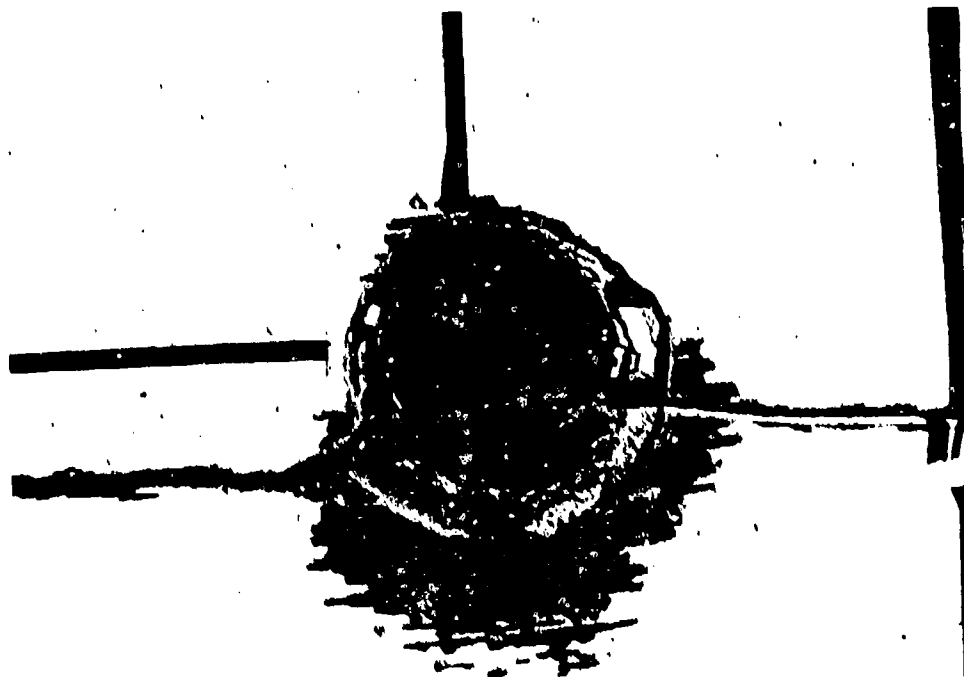


Figure 3.5. Close-up of impact area in FDI, showing front surface spall and cracks.



Figure 3.6. Panel from FDI2 (7LF1.75), illustrating crack pattern in a foam-backed panel.

The graphite epoxy panels also exhibited sudden failure thresholds; however, the crack system followed the 0/45/90 layup of the fibers. The crack first ran horizontal and then branched to follow the fiber directions. Figure 3.7 illustrates a typical failure of a graphite epoxy panel.

Definition of failures in the stiffened panels was not so straightforward. The damage that occurred included cracking of the panel, crushing of the stiffeners, and "popping" of rivets. The cracks in the panel were always stopped by the stiffeners. Cracks that started to travel vertically between stiffeners travelled only a relatively short distance before turning into a stiffener and stopping. Figure 3.8 illustrates these features of the failure. The panel in the figure was massively torn near the impact site, but the cracks did not run more than about ten projectile diameters. Addition of foam alleviated the entrance panel failure. Foam had no discernable effect on stiffener crushing and rivet failure, as shown in Figures 3.9 and 3.10. However, the foam was not attached to the stiffeners themselves (see Section 2.2).



Figure 3.7. Failure in a graphite epoxy panel.

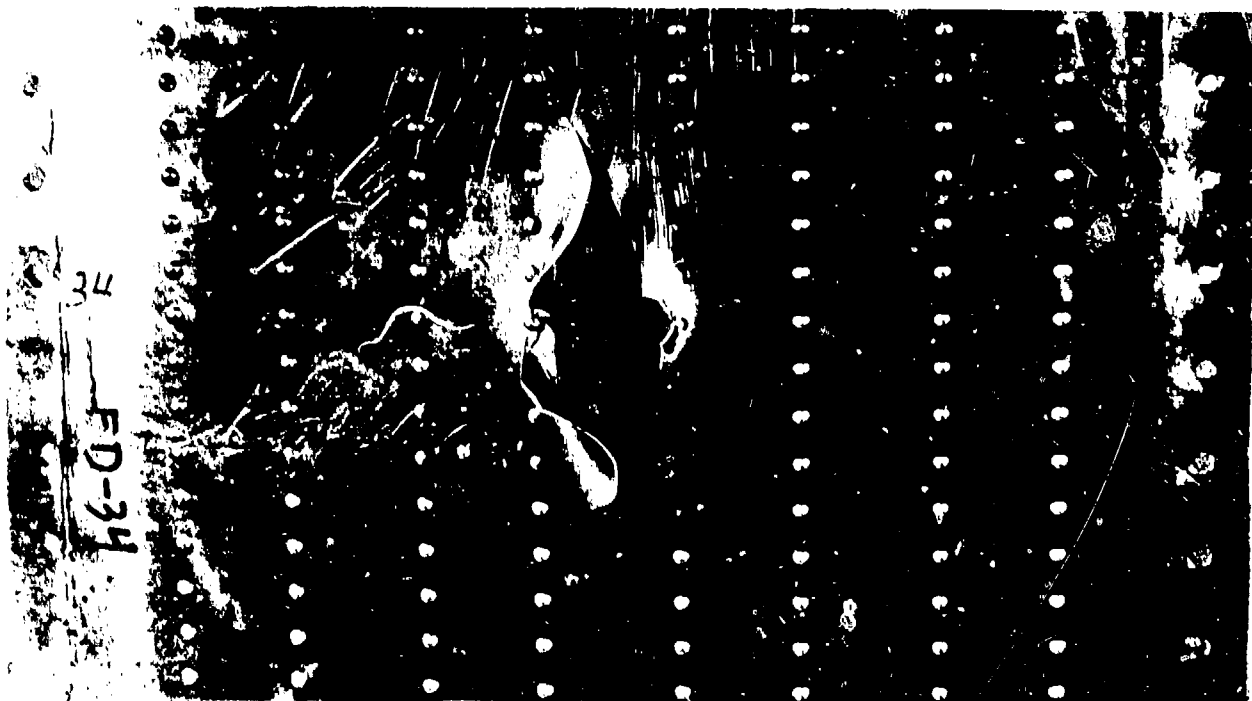


Figure 3.8. Panel from Shot FD13 ( $\hat{2}L1.49$ ), showing how stiffeners prevented massive tearing.

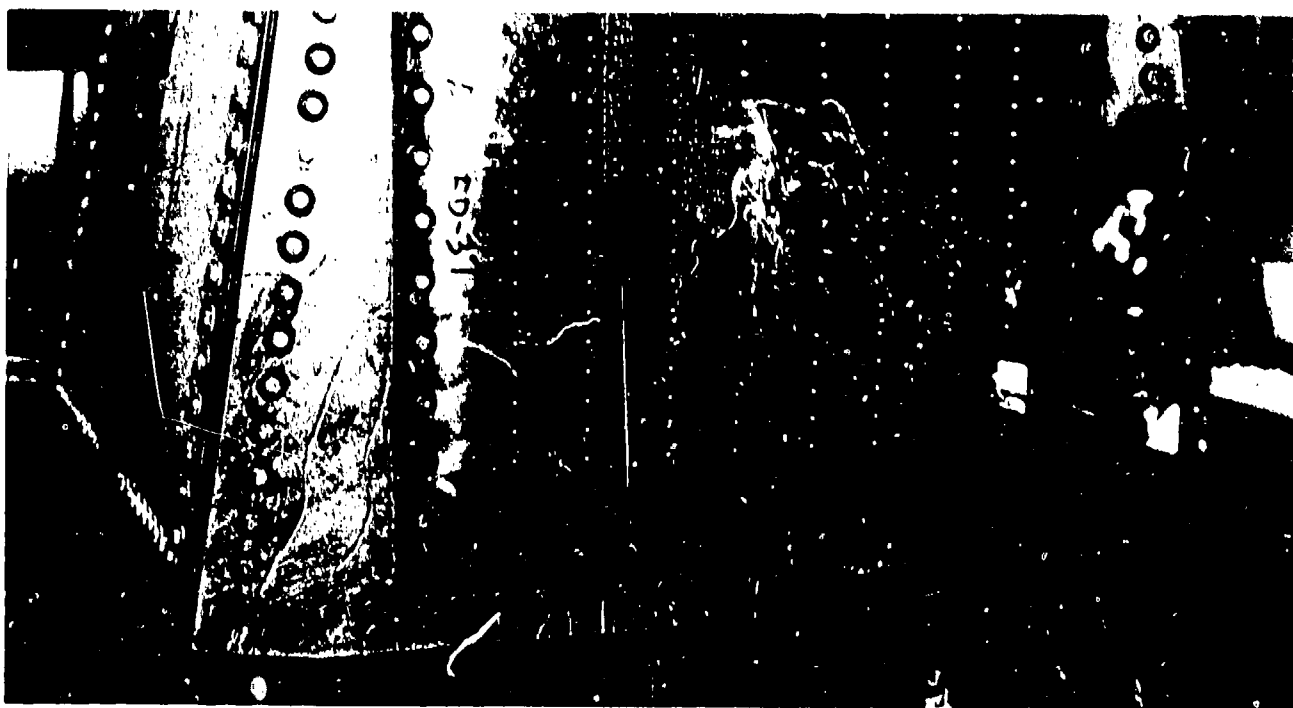


Figure 3.9. Panel from Shot FD37 ( $\hat{2}LF1.56$ ). The foam had little effect on the rivet failures. Note: foam did not cover rivets (see Section 2.2).

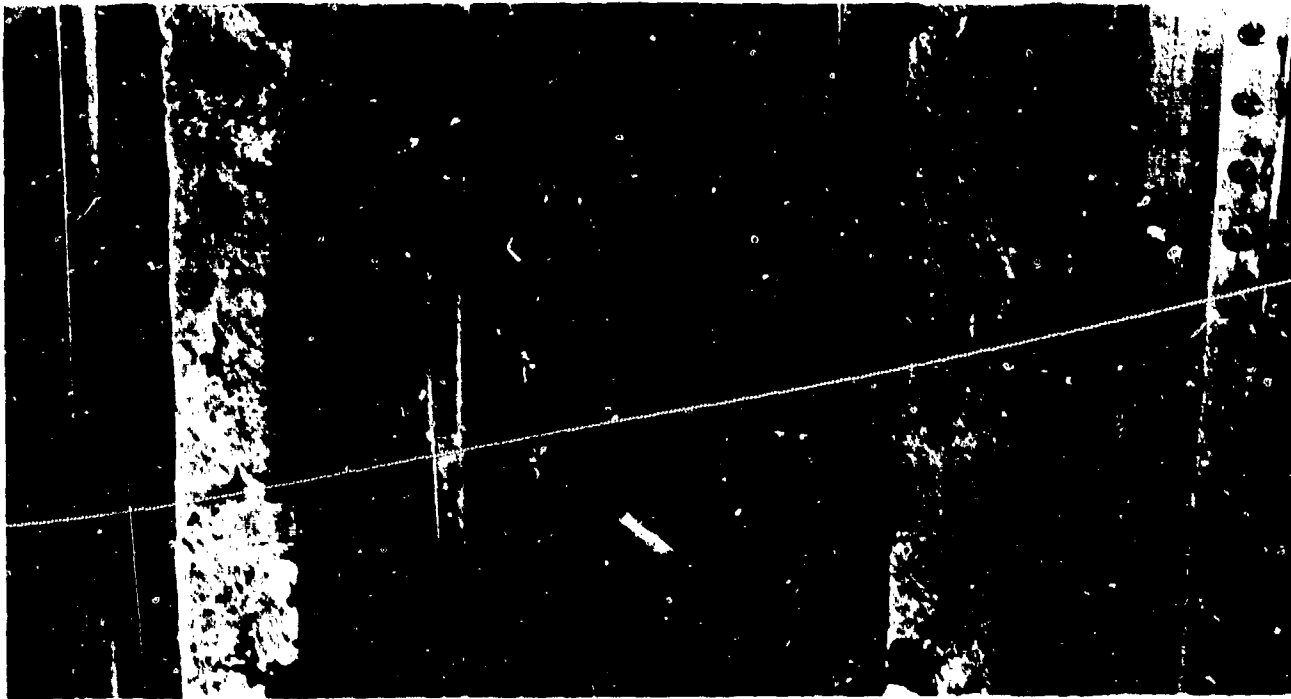


Figure 3.10. Rear of panel from Shot FD37, showing crushing of stiffeners.

There was an excellent correlation between the final deflection\* of a stiffener and the number of rivets popped. At 10-mm displacement or less, no rivets popped; at 15-mm displacement or more, 8 or more rivets popped. This phenomenon provided a convenient way to define failure, since the number of rivets failed was always none or more than seven. According to this definition, a "failed" stiffened panel was one whose ability to support compressive loads had been severely reduced by damage to one or more reinforcing elements. No stiffened panel failed in such a way as to cause massive liquid leakage (as occurred in unstiffened panels) or serious degradation of the tensile properties of the structure.

Some insight into the failure mechanism resulted from Shot FD35, in which one of the stiffeners next to the impact site accidentally filled with water. That stiffener was not deformed

\*Final deflection of a stiffener was measured by laying a straight edge along the top of the stiffener and measuring the maximum distance between the straight edge and stiffener surfaces.

and no rivets were popped. This result indicates that rivet failure was due to stiffener collapse (e.g., motion in the x-direction), not panel displacement.

The velocity of propagation and the time of initiation of the cracks were determined for a few representative cases. For the bare thin 7075-T6 panels (namely FD15, 7S1.59), the velocity steadily decreased. For the first few frames after impact, the velocity was  $\sim 300$  m/s; however, beyond about 150 mm from impact, it slowed to about 200 m/s. The delay time between impact and crack initiation for both thin and thick bare panels could not be resolved; i.e., these events were simultaneous within an uncertainty of about 100  $\mu$ s. For example, Figure 3.11 shows the first frame after impact in Shot FD23. (This is within 180  $\mu$ s after impact.) The cracks have already grown to a length of over 60 mm. Crack initiation was not always prompt in failures of foam-backed 1.6-mm 7075-T6 panels. The cracks started between 360  $\mu$ s and 720  $\mu$ s after impact, and in Shot FD14 they started between 0 and 350  $\mu$ s after impact.

### 3.1.2 Perforation Data

The size of the perforation hole in the target panels was dependent on impact velocity. The data for 7075-T6 aluminum are given in Table VI. Where the hole was not round, we used an equivalent diameter, computed from  $\sqrt{4A_h/\pi}$ , where  $A_h$  was the hole area.

The data are displayed graphically in Figure 3.12 in which  $d/W$  (hole diameter scaled by panel thickness) is plotted against impact velocity. Plotted in this format, the data fall into two groups, with the thick panel data lying below the thin panel data. For thin panels,  $\partial d/\partial W|_{W,D} > 0$ , as one would expect. However, for the thick panels, the dependence of  $d$  on  $W$  is so small that it is only marginally detectable from the figure.

The hole diameter in the thin panels was substantially decreased by the presence of foam. Compare 7S and 7SF. The hole formation proceeded by a crater growth process that was terminated by release waves from the aluminum/foam interface.

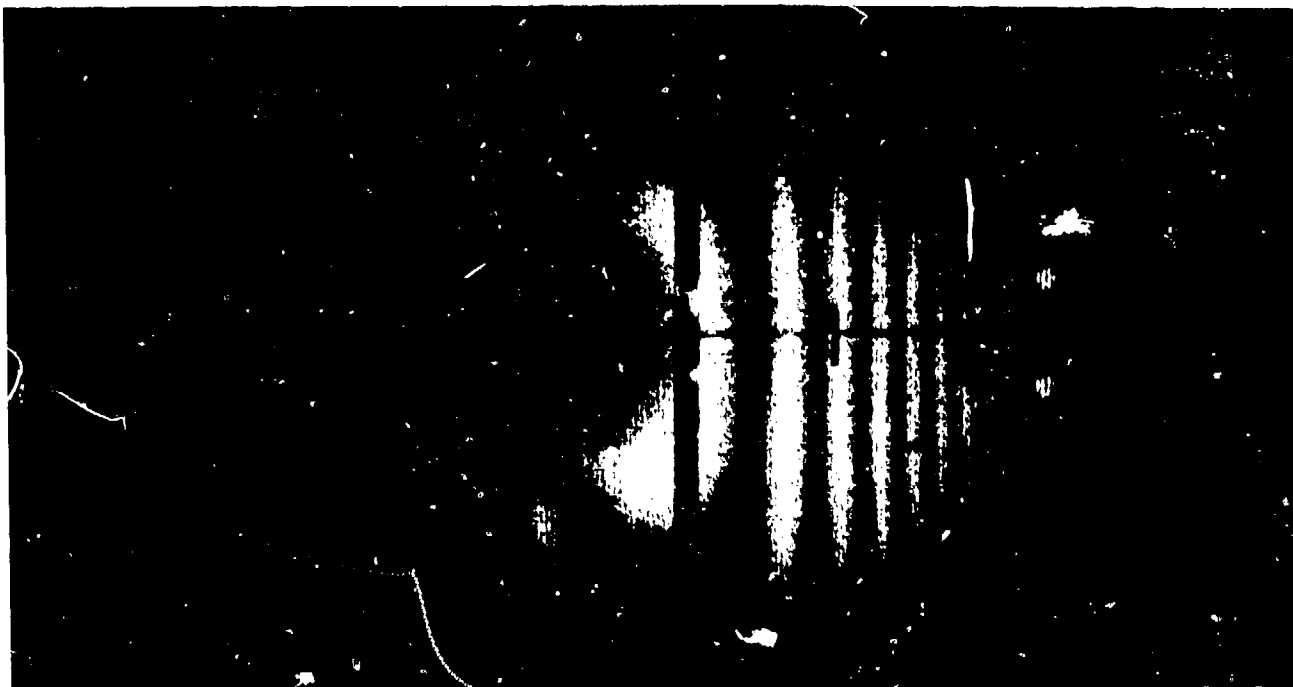


Figure 3.11. Second frame after impact in Shot FD23 (7S1.35). Camera speed was 5.5 frames/ms, and horizontal crack is already clearly visible.

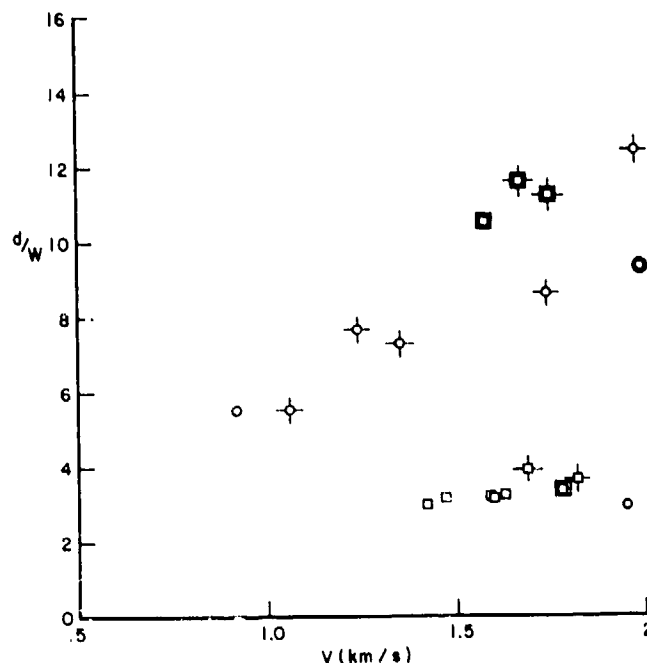


Figure 3.12. Hole diameter scaled by panel thickness versus impact velocity. Square symbols show thick panels and round symbols show thin panels. Nested symbols indicate foam. Radial lines indicate failure.

TABLE VI. HOLE SIZE DATA

Shot No.	Configuration	V	d	$\frac{d}{\bar{w}}$
1	7L	1.82	23.4	3.69
2	"	1.42	18.3 {19.0	2.99
3	"	1.42	19.7	
4	"	1.47	20.1	3.17
5	"	1.60	20.2	3.18
6	"	1.69	24.9	3.92
7	"	1.59	20.6	3.24
8	"	1.63	20.6	3.24
9	7LF	-----	19.66	3.10
10	"	-----	18.96	2.99
11	"	1.78	21.50	3.39
12	7LF	1.75	18.67	11.29
13	7LF	1.58	16.82	10.54
14	7LF	1.67	18.60	11.63
15	7S	1.98	19.97	12.48
16	7SF	1.99	15.02	9.39
17	2L	-----	17.76	2.80
18	"	-----	17.11	2.69
19	7S	1.95	18.47	2.95
22	7S	1.74	13.84	8.65
23	7S	1.35	11.66	7.29
27	7S	1.24	12.31	7.69
28	7S	0.92	8.86	5.54
29	7S	1.06	8.86	5.54

For thin panels, hole size correlated with failure, all else being equal. The thin panels failed when  $d/W$  exceeded about unity. Hole size varied little on the thick panels. The data support the statement that failure occurred when  $d/W$  exceeded about 1.65.

In two out of three cases (see Figure 3.12), maximum hole size occurred at velocities slightly higher than the failure threshold velocities. This may be analogous to a well-known phenomenon of conventional terminal ballistics: the maximum energy deposition in a target plate occurs at the limit velocity. This is consistent with the trend that projectile fragmentation had a slight maximum at velocities just below failure threshold velocity.

Do large holes induce radial cracks at the impact site? If they do, then analysis of hole formation might lead to a useful failure prediction. The mechanism by which foam protects panels from gross damage would also be clarified. Even if large holes do not induce failure, a failure prediction algorithm might be based on a prediction of hole size and an empirical correlation. However, a relatively simple analysis can show that hole size is not causally related to fracture. Consider, for example, a thin plate with a round hole; and let the plate be subjected to a radial tension  $P$  far from the hole. This situation is analogous to the early-time loading in a target panel; the water pressure acts on an annular region around the hole, while the region immediately adjacent to the hole experiences no external loading because of the cavity behind the projectile. The stress in the panel at the hole boundary in this approximation is given by

$$\sigma_r = P (1 - d^2/r^2) \quad (1)$$

$$\sigma_\theta = P (1 + d^2/r^2) \quad (2)$$



Thus, the hoop stress at the hole,  $\sigma_{\theta}$  ( $r=d$ ), is equal to  $2P$ , regardless of  $d$ . In other words, the hole size does not influence the maximum tensile stress at its boundaries. (Of course, the hole shape will affect the stress distribution. That is why cracks initiate at the corners of the square holes.

### 3.2 PRESSURE DATA

Free-field pressure data were obtained in most shots. The form of the pressure time curves was strongly influenced by panel geometry; however, it was only weakly dependent on projectile size. In this section we present the data together with some analysis. Additional analysis is contained in Section IV in which the data are compared with the numerical results.

We first discuss the data for pressure variations behind thick (6.35 mm) aluminum panels. The set of data available pertains to the 11.67-g cubical projectiles. Most attention was directed to the  $r = 100$  mm station, and usable data were obtained for that transducer in every shot. Figures 3.13 - 3.17 show how the measured pressure pulse varied with impact velocity. For reference, Figure 3.18 is a sketch that illustrates individual features common to all the traces. These are labelled as follows:

- E1 - a small precursor signal
- P1 - first sharp peak associated with first significant arrival
- P2 - second peak, often composed of several subpeaks
- P3 - third broad peak
- R2 - initial sharp fall that marks the end of P2
- R3 - sharp fall from third peak value

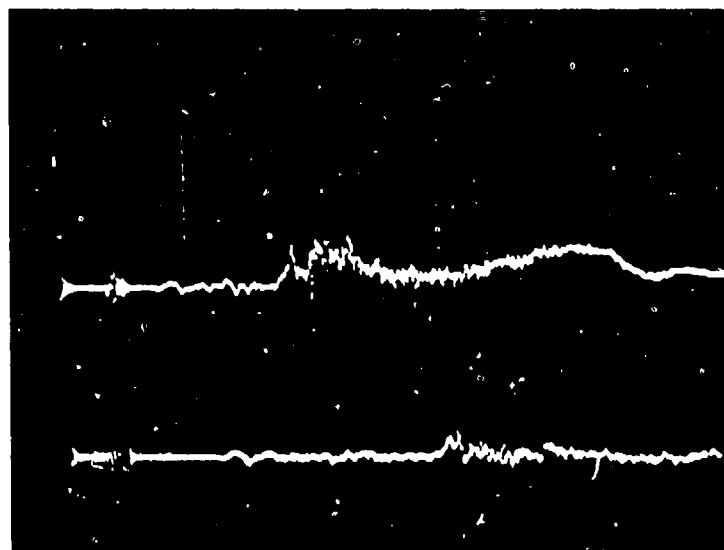


Figure 3.13. Upper trace is  $r=100$  mm pressure transducer from Shot PD3. Note: Scale marks indicate  $50 \mu s$  and  $0.41$  kbar (6 ksi). Lower trace is  $r=200$  mm transducer at the same sweep speed.

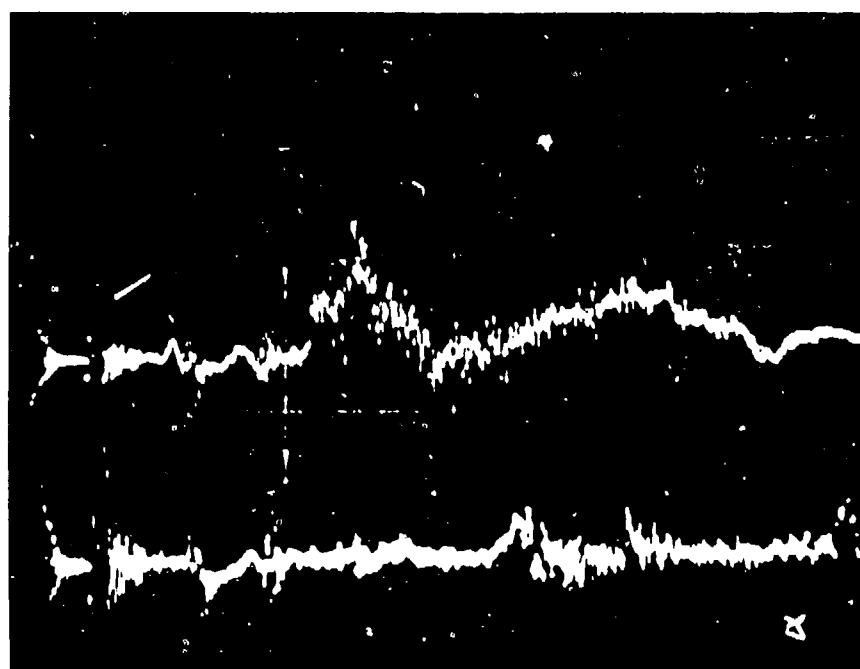


Figure 3.14. Upper trace is  $r=100$  mm pressure transducer from Shot PD4. Note: Scale marks indicate  $50 \mu s$  and  $0.41$  kbar (6ksi). Lower trace is  $r=200$  mm transducer at the same sweep speed.

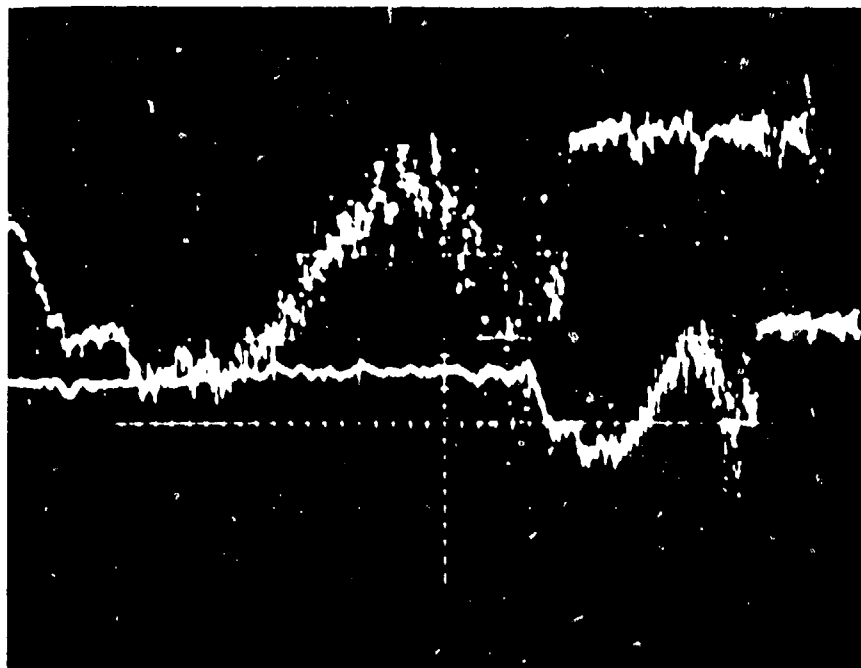


Figure 3.15. Record for  $r=100$  mm transducer from Shot FD5. Note: Upper trace sweep is  $50 \mu\text{s}/\text{division}$ ; lower trace sweep is  $20 \mu\text{s}/\text{division}$ . Upper trace amplification is  $105 \text{ bar}/\text{division}$  ( $1.53 \text{ ksi}/\text{division}$ ); lower trace amplification is  $52.8 \text{ bars}/\text{division}$  ( $766 \text{ psi}/\text{division}$ ).

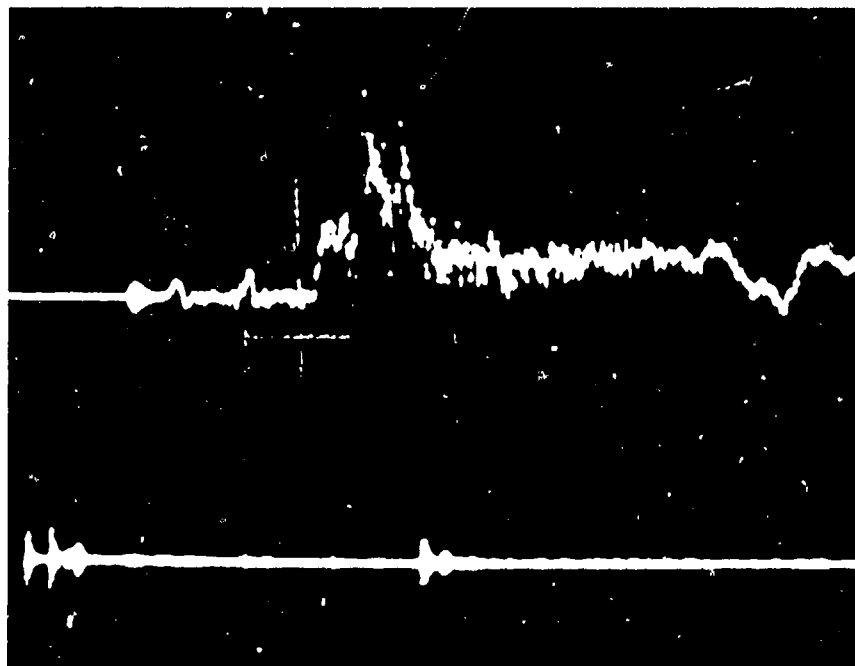


Figure 3.16. Record from  $r=100$  mm transducer from Shot FD8. Note: Scale marks indicate  $50 \mu\text{s}$  and  $0.41 \text{ kbar}$  ( $6 \text{ ksi}$ ).

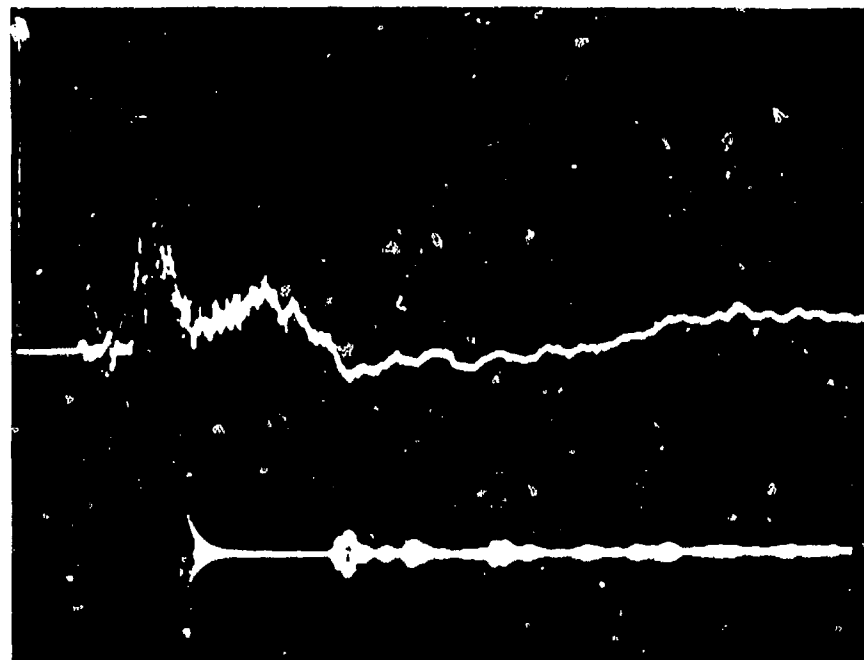


Figure 3.17. Record from  $r=100$  mm pressure transducer from Shot FD6. Note: Scale marks indicate  $50 \mu s$  and  $0.41$  kbar ( $6$  ksi).

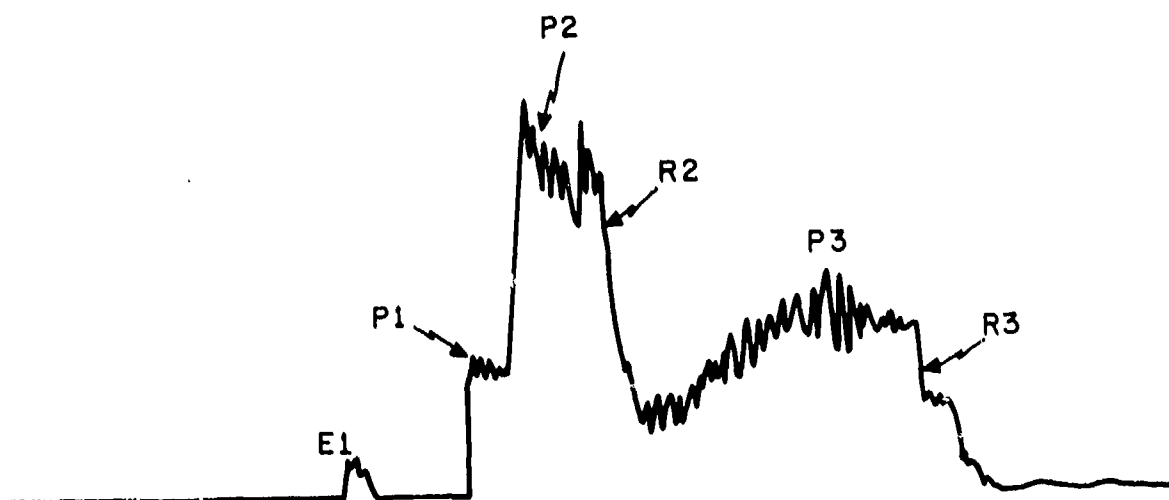


Figure 3.18. Sketch of general form of  $r=100$  mm pressure records.

Figure 3.19 illustrates typical data from an  $r = 75$  mm transducer. The details of these traces were also very consistent in the shots for which good data were obtained, namely FD3, 4, 5, and 8. In FD3 and 4, two side-by-side transducers were placed at this station, and the resulting pressure traces were essentially identical. All the  $r = 75$  mm data show three arrivals of similar amplitudes at intervals of about  $10 \mu s$ .

Data obtained from the pressure records are summarized in Tables IV, V, and VI. Table VII contains the arrival time data for the  $r = 100$  mm station. The uncertainty in the instant of impact prevents an accurate estimate of absolute arrival times. The P1, R2, and R3 arrival times were measured from the first detectable signal. The P2 arrival was measured from half way up the first rise in pressure for that pulse; this is the most widely accepted procedure for determining shock arrival times. Relative arrival times are very consistent, but no correlations with velocity were discovered. The average and standard deviation for relative arrival times are  $8.66 \pm 0.75 \mu s$  for  $P2 - P1$ ,  $90.36 \pm 4.54 \mu s$  for  $R3 - P2$ , and  $99.02 \pm 3.83$  for  $R3 - P1$ . Our interpretation of these events is that E1 arises from the elastic wave that runs out in the aluminum plate, and P1 results from energy that is coupled to the aluminum by the shock in the water. (see Section 4.1). P2 is the wave that results from the strong shock that propagates ahead of the projectile through the aluminum and into the water, where it produces an approximately hemispherical pressure wave. We believe the third pressure pulse is caused by the drag force on the fluid after the projectile and wall fragments entered the water. The most solid argument for this interpretation is the relatively late arrival of this pulse, typically  $40 \mu s$  after P1, and its long duration; since the projectile takes less than  $5 \mu s$  to perforate the target plate, this pressure must be caused by the projectile-fluid drag force. The variability of the P3 amplitude is presumably caused by variations in the way that the projectile broke up. The end of the P3 pulse is probably caused by the arrival of the cavity at the transducer.

Table VIII contains the data for peak pressure behind 6.35-mm-thick aluminum panels. The decay of the P1 pressure with range is shown graphically in Figure 3.20. In this figure we note that the value at  $r = 20$  cm for FD8 is highly anomalous. We believe the recorded scope setting is probably erroneous. The limited data set suggests an  $r^{-2}$  fall-off. The impulses obtained by integrating the pressure traces are given in Table IX. Some of the error brackets are asymmetric; they resulted from uncertainties concerning base line shifts in the pressure records\*. The impulse variations with range are discussed more fully in Section IV.

Figure 3.21 shows the result of adding foam (configuration 7LF) to the thick panels. Two of the peaks have been suppressed at both radii. This supports the assertion that the first peak is caused by a shock wave in the aluminum, since the foam decouples the aluminum from the water.

The pressure traces in thin panels also did not contain multiple arrivals. Figure 3.22 shows what happens when the thick panel is replaced by a thin one, e.g., configuration 7S. The waves associated with propagation in the aluminum are absent or greatly attenuated. All pressure traces from the extensive 7S series of shots had the form of Figures 3.23 and 3.24. The pressure pulses must be mainly the result of the interaction of the projectile and water.

Figure 3.23 shows the effect of adding foam to this configuration (e.g., 7SF). The arrival is sharper, since the small signals caused by waves in the panel have been eliminated. The duration has also been slightly reduced; however, the peak and impulse have been only slightly affected.

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\*When base line shifts occurred, it was assumed that they were linear, starting at the P1 arrival and ending at the conclusion of P3. Apparently both positive and negative shifts occurred. Negative shifts can be caused by high temperatures. The cause of positive shifts has not yet been determined.

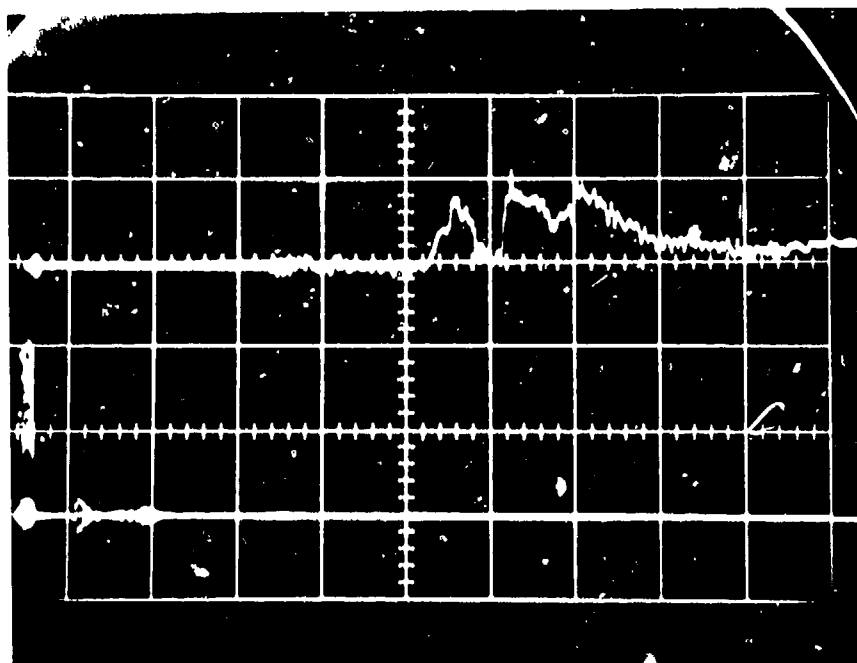


Figure 3.19. Record from  $r = 75$  mm pressure transducer from Shot FD8. Note: Sweep speed is  $10 \mu\text{s}/\text{division}$ , and amplification is  $258 \text{ bars}/\text{division}$  ( $3.75 \text{ ksi}/\text{division}$ ). (The amplification setting is suspect in this record.)

TABLE VII. SUMMARY OF ARRIVAL TIME DATA AT  $r = 100$  mm,  $z = 6$  mm FOR 6.35-mm PLAIN 7075-T6 ALUMINUM PANELS STRUCK BY 11.66-g CUBES. TIMES ARE  $\mu\text{s}$  AFTER BEGINNING OF TRACE.\*

Shot No.	Impact Velocity (km/s)	Estimated Impact	P1 Arrival	P2 Arrival	R2 Arrival	R3 Arrival
FD3	1.42	13.4	$68.3 \pm 0.5$	$77.2 \pm 0.5$	$89.3 \pm 0.6$	$165.2 \pm 0.5$
FD4	1.47	13.0	$62.4 \pm 0.5$	$70.2 \pm 0.5$	$88.1 \pm 0.6$	$164.4 \pm 0.5$
FD5	1.60	11.9	$56.3 \pm 0.8$	$64.6 \pm 0.8$	$76.7 \pm 0.9$	$159.4 \pm 0.8$
FD8	1.63		$71.1 \pm 0.2$	$80.7 \pm 0.3$	$92.2 \pm 0.3$	$165.7 \pm 0.3$
FD6	1.69	14.1	$64.8 \pm 0.5$	$73.7 \pm 0.8$	$91.0 \pm 0.8$	$162.3 \pm 0.8$

\*The instant of impact was not well enough determined to be useful as a time reference for arrival time comparisons.

TABLE VIII. SUMMARY OF PEAK PRESSURE DATA FOR 6.35-mm PLAIN 7075-T6 ALUMINUM PANELS STRUCK BY 11.66-g CUBES. TRANSDUCERS WERE 6 mm BEHIND TARGET PANELS. (NOTE 1 ksi = 0.069 kbar).

Shot No.	$u_o$ (km/s)	Radius (cm)	First Peak (ksi)	Second Peak (ksi)	Third Peak (ksi)
FD3	1.42	7.5	$2.65 \pm 0.25$	$2.61 \pm 0.26$	$1.92 \pm 0.17$
		10	$1.59 \pm 0.16$	$1.72 \pm 0.16$	$1.64 \pm 0.13$
		20	---	$0.363 \pm 0.36$	---
FD4	1.47	7.5	$2.45 \pm 0.30$	$2.37 \pm 0.16$	$1.57 \pm 0.13$
		10	$1.08 \pm 0.05$	$1.78 \pm 0.15$	$1.11 \pm 0.05$
		20	---	$0.302 \pm 0.019$	---
FD5	1.60	7.5	$4.58 \pm 0.20$	$3.43 \pm 0.30$	$3.33 \pm 0.20$
		10	$1.49 \pm 0.09$	$2.24 \pm 0.20$	$2.31 \pm 0.38$
FD8	1.63	7.5	$2.91 \pm 0.12$	$2.95 \pm 0.12$	$3.33 \pm 0.25$
		10	$1.17 \pm 0.10$	$2.70 \pm 0.17$	$0.59 \pm 0.05$
		20	---	$6.90 \pm 1.0$ $- 0.4$	---
FD6	1.69	10	$1.86 \pm 0.08$	$2.06 \pm 0.20$	$1.32 \pm 0.12$

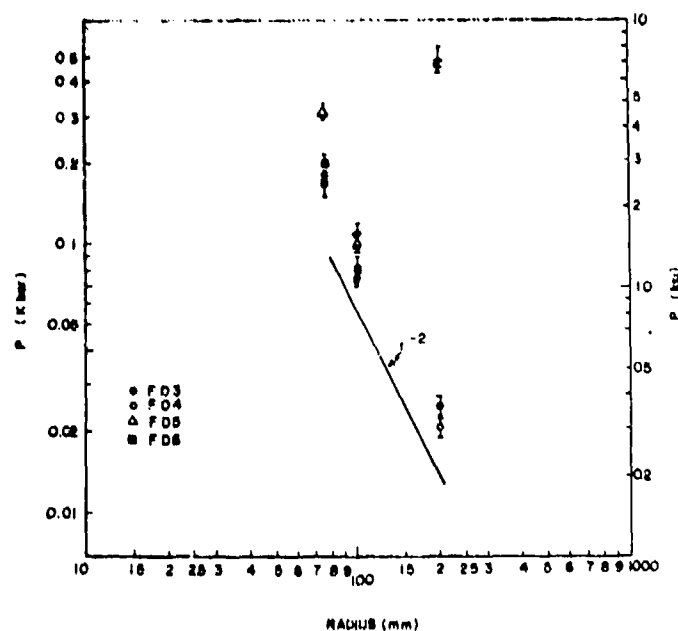


Figure 3.20. Decay of  $P_1$  with range for 7L configuration.



TABLE IX. SUMMARY OF IMPULSE RESULTS AT  $r = 100$  mm,  $z = 6$  mm  
FOR 6.35-mm 7075-T6 PLAIN PANELS STRUCK BY 11.66-g  
CUBES. UNITS ARE  $\text{cgs}$  ( $10^{-6}$  bar-s)

Shot No.	Impact Velocity	Impulse in P1 and P2 Peaks	Time at End of P2*	Impulse in P1, P2, and P3 Peaks	Time at End of P3*
FD3	1.42	2210 $\pm$ 270	34.4	6180 $\pm$ 480	103.6
FD4	1.47	1830 $\pm$ 60	29.5	4910 $\pm$ 90	106.7
FD5	1.60	2860 $\pm$ 300 - 140	36.4	10650 $\pm$ 4300 - 100	130.0
FD8	1.63	2910 $\pm$ 80	27.7	5800 $\pm$ 60	105.0
FD6	1.69	2790 $\pm$ 180 - 50	35.9	8330 $\pm$ 1980 - 180	124.0

\*Measured in  $\mu\text{s}$  after P1 arrival.

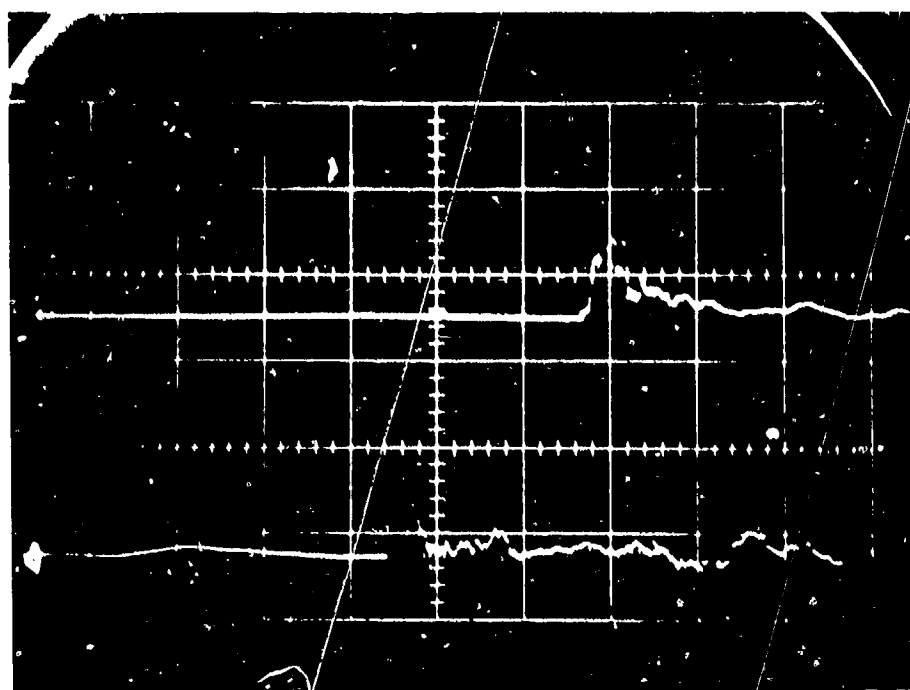


Figure 3.21. Upper trace: Pressure data from Shot FD11 (configuration 2LF, at  $r = 7.5$  cm. Note: 10  $\mu\text{s}$  div; 153 bars div. Lower trace: Pressure data at  $r = 10$  cm. 20  $\mu\text{s}$  div; 106 bars div.

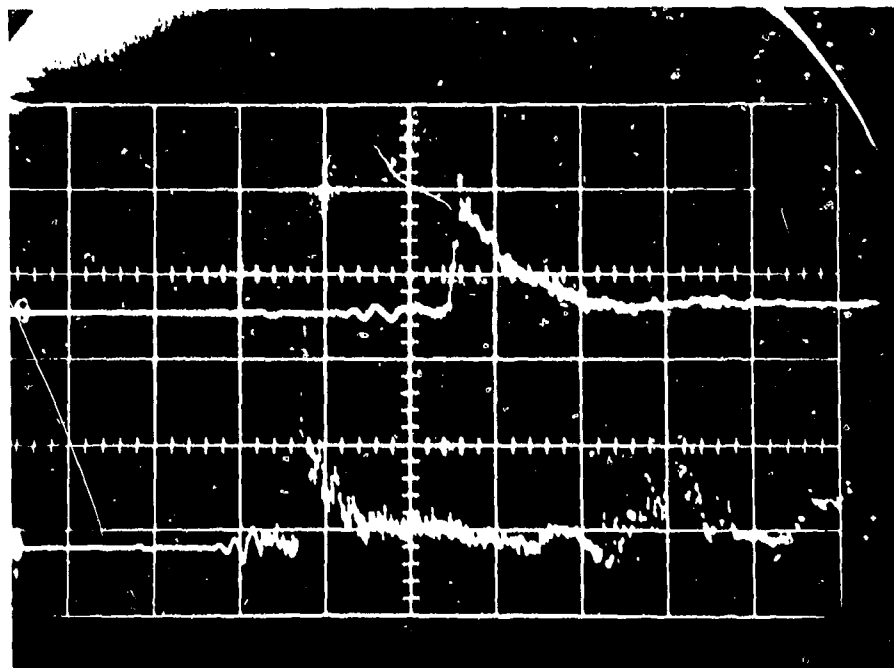


Figure 3.22 Upper trace: Pressure data from Shot FD15 (7S1.98), at  $r = 75$  mm.  $10 \mu\text{s}/\text{div}$ ; 258 bars/div. Lower trace: Pressure data at  $r = 100$  mm.  $20 \mu\text{s}/\text{div}$ ; 106 bars/div.

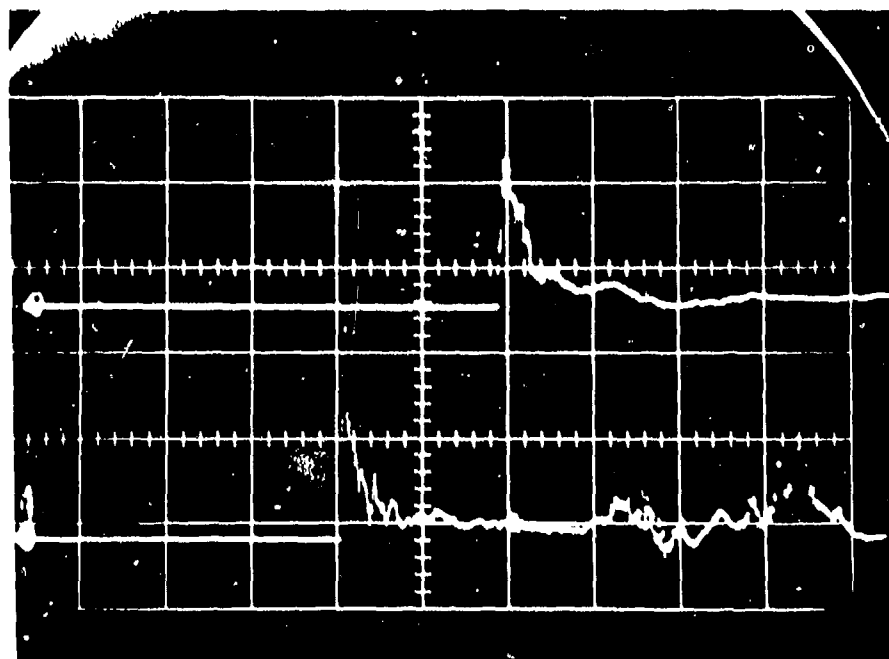


Figure 3.23. Upper trace: Pressure data from Shot FD16 (7SF1.99), at  $r = 75$  mm.  $10 \mu\text{s}/\text{div}$ ; 258 bars/div. Lower trace: Pressure data at  $r = 100$  mm.  $20 \mu\text{s}/\text{div}$ ; 106 bars/div.

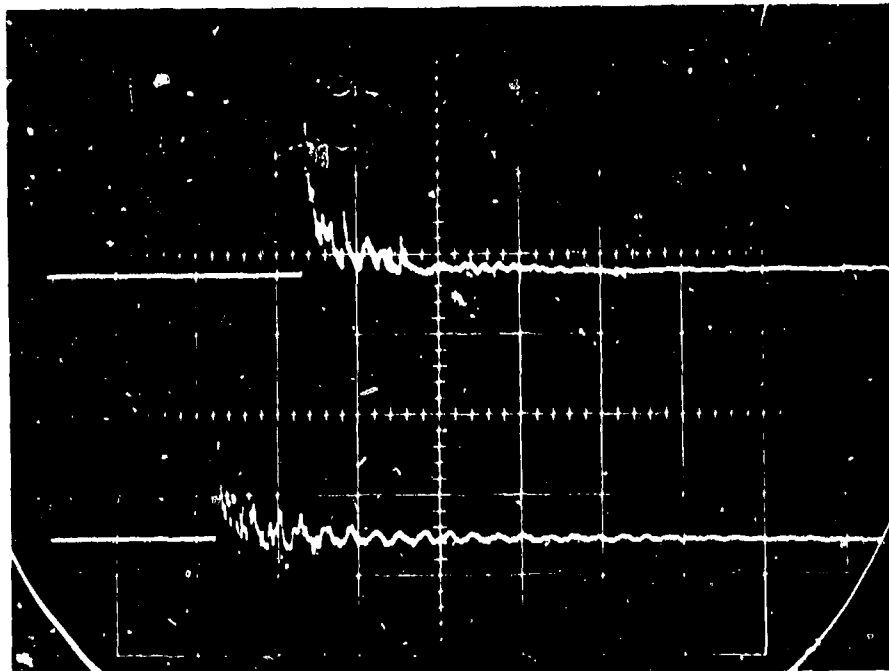


Figure 3.24. Pressure records from Shot FD22 (7S1.74), 50  $\mu$ s/division. Note: Upper trace:  $T_5$  transducer, 85 bars/division. Lower trace:  $T_4$  transducer, 657 bars/division.

Table X summarizes the measurements of peak pressure in Shots FD11 through FD19. We tried plotting the data of Table X in various formats to help reveal latent empirical relationships. No outstanding global relationships were identified. At  $r = 10$  cm, peak pressure varied remarkably little with configuration or impact velocity; the mean of all the data was 147 bars, and the standard deviation was only 20 percent. The  $r = 7.5$  cm data contained only slightly more velocity dependence. For example, at  $r = 7.5$  cm, foam had very little effect on peak pressure (e.g., Shots 15 versus 16, or 13 and 14 versus 8 and 6). One trend was that at 7.5 cm in plain panels for similar projectiles, thin panels experience higher pressures than thick ones. This trend is reversed at  $r = 10$  cm. As observed previously, failure had little effect on peak pressure.

TABLE X. SUMMARY OF PEAK PRESSURE DATA  
(1 ksi=69 bars=6.9 MN/m<sup>2</sup>)

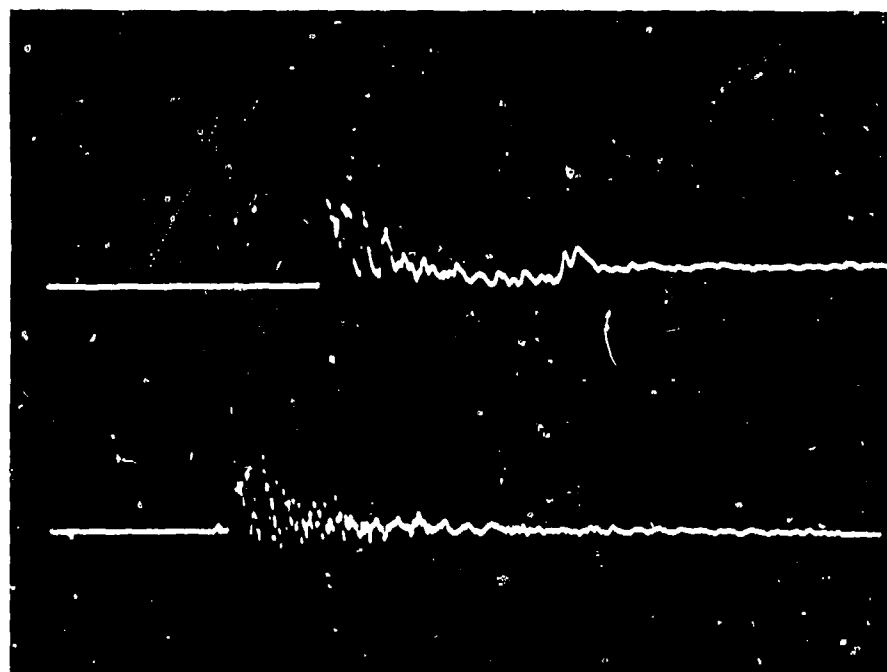
Configuration	Shot No.	$U_0$ (km/s)	$r$ (cm)	$P_1$ (ksi)	$P_2$ (ksi)	$t_1$ ( $\mu$ s)	$t_2$ ( $\mu$ s)	Failure
7LF	FD11	1.78	7.5	$3.60 \pm 0.2$	$1.10 \pm 0.85$	$1.97 \pm 0.15$	$5.22 \pm 0.28$	N
			10	$1.52 \pm 0.16$		$0.85 \pm 0.28$		
7LF	FD13	1.58	7.5	$3.07 \pm 0.21$	$1.19 \pm 0.10$	$1.13 \pm 0.07$	$4.58 \pm 0.14$	N
			10	$1.93 \pm 0.25$	$1.08 \pm 0.16$	$2.26 \pm 0.28$	$7.34 \pm 0.28$	
	FD14	1.67	7.5	$3.22 \pm 0.2$	$2.43 \pm 0.4$	$1.90 \pm 0.14$	$4.37 \pm 0.14$	Y
			10	$1.69 \pm 0.32$	$1.05 \pm 0.09$	$1.69 \pm 0.28$	$9.03 \pm 0.28$	
7S	FD12	1.75	7.5	$5.62 \pm 0.40$	$2.62 \pm 0.10$	$1.69 \pm 0.14$	$5.71 \pm 0.07$	Y
			10	$2.40 \pm 0.40$	$1.70 \pm 0.16$	$1.27 \pm 0.28$	$5.93 \pm 0.56$	
	FD15	1.98	7.5	$4.05 \pm 0.4$	$3.25 \pm 0.2$	$1.91 \pm 0.14$	$4.37 \pm 0.14$	Y
			10	$2.10 \pm 0.4$	$1.32 \pm 0.16$	$0.99 \pm 0.56$	$9.17 \pm 0.56$	
7SF	FD16	1.99	7.5	$4.73 \pm 0.4$	$1.19 \pm 0.40$	$0.99 \pm 0.14$	$4.30 \pm 0.28$	N
			10	$2.04 \pm 0.4$	$0.76 \pm 0.16$	$0.85 \pm 0.56$	$7.48 \pm 0.28$	
7S	FD19	1.95	7.5	$2.01 \pm 0.20$	$2.70 \pm 0.20$	$1.90 \pm 0.28$	$10.15 \pm 0.14$	N
			10	$1.61 \pm 0.4$	$2.87 \pm 0.40$	$2.26 \pm 0.56$	$18.33 \pm 0.28$	
					$2.44 \pm 0.4$		$17.2 \pm 0.56$	

Configuration nomenclatures are explained in Table I.

$P_1$  and  $P_2$  refer to first and second peaks.

$t_1$  and  $t_2$  are time intervals between the arrival of the main signal and the peak pressures.

Pressure pulses at the T4 and T5 transducer positions, which were respectively 10 and 20 cm behind the panel, were relatively unaffected by the panel construction. Representative data are shown in Figures 3.24, 3.25, and 3.26. In the 7S and GS records, the main arrival is preceded by shock. The central frequency of the shock is above the transducer frequency response, so the amplitudes on the traces are not quantitative. The shock duration was only a few microseconds, while the main pulse duration was  $\sim 40 \mu\text{s}$  at T<sub>4</sub> and  $\sim 75 \mu\text{s}$  at T<sub>5</sub>. The pressure decayed by a factor of about 4 between those two gauges; the ratio of radial distance was 1.58. The shock spike was missing in the data from foam-backed panels (Figure 3.26). There are two possible causes for the spike reduction: (1) the more immediate shock relief at the foam/fluid interface, and (2) the fact that the impact stress resulting from steel striking water is less than the impact stress resulting from steel striking aluminum which strikes water.



3.25. Pressure records from Shot FD25 (GS1.46), 50  $\mu\text{s}$ /division. Note: Upper trace: T<sub>5</sub> transducer, 43 bars/division. Lower trace: T<sub>4</sub> transducer, 657 bars/division. Picture magnification is the same as Figure 3.24.

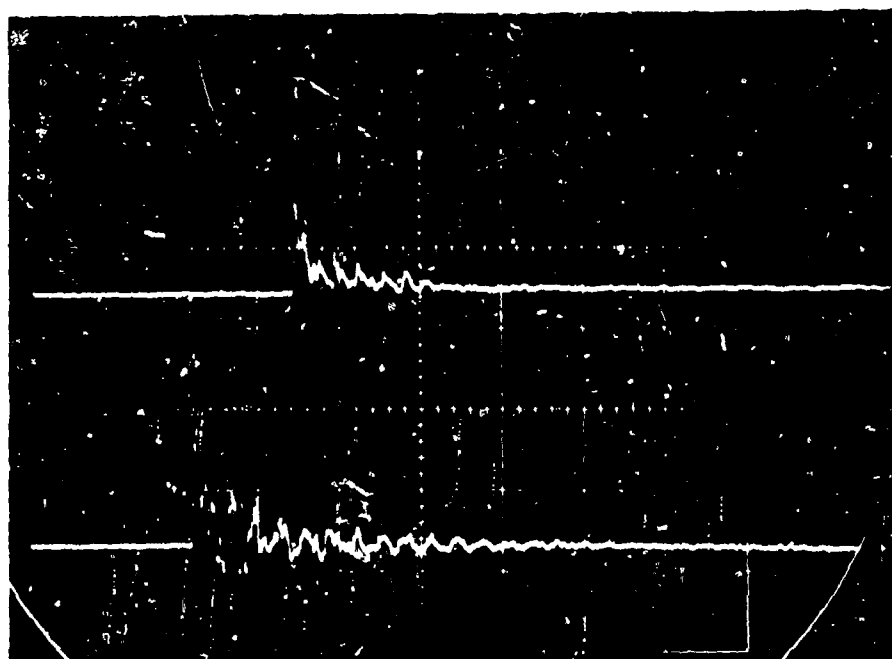


Figure 3.26. Pressure records from Shot FD16 (7SF1.99), 50  $\mu$ s/division. Note: Upper trace: T<sub>5</sub> transducer, 170 bars/division. Lower trace: T<sub>4</sub> transducer, 657 bars/division.

### 3.3 DISPLACEMENT DATA

Moiré fringe data were taken for most shots. In general, the data were analyzed for shots in which no failure occurred. In this way, data for peak displacement were obtained that could be compared with the calculations.

#### 3.3.1 Discussion of Thick Aluminum Data

CRT did only one calculation of panel displacement. The calculation corresponded to Shot FD8; hence, the shot was analyzed in more detail than any other shot. Shot FD8 was made with the 7L configuration at a velocity just below the failure threshold. The first eight frames from the moiré fringe record are shown in Figure 3.27.

Figure 3.28 shows the shape of the panel (along a horizontal line) until maximum displacement occurred. The duration of the obscuration due to impact flash was only about 0.5 ms. In places where broad fringes resulted in ambiguous data, dashed lines show alternate interpretations. The maximum

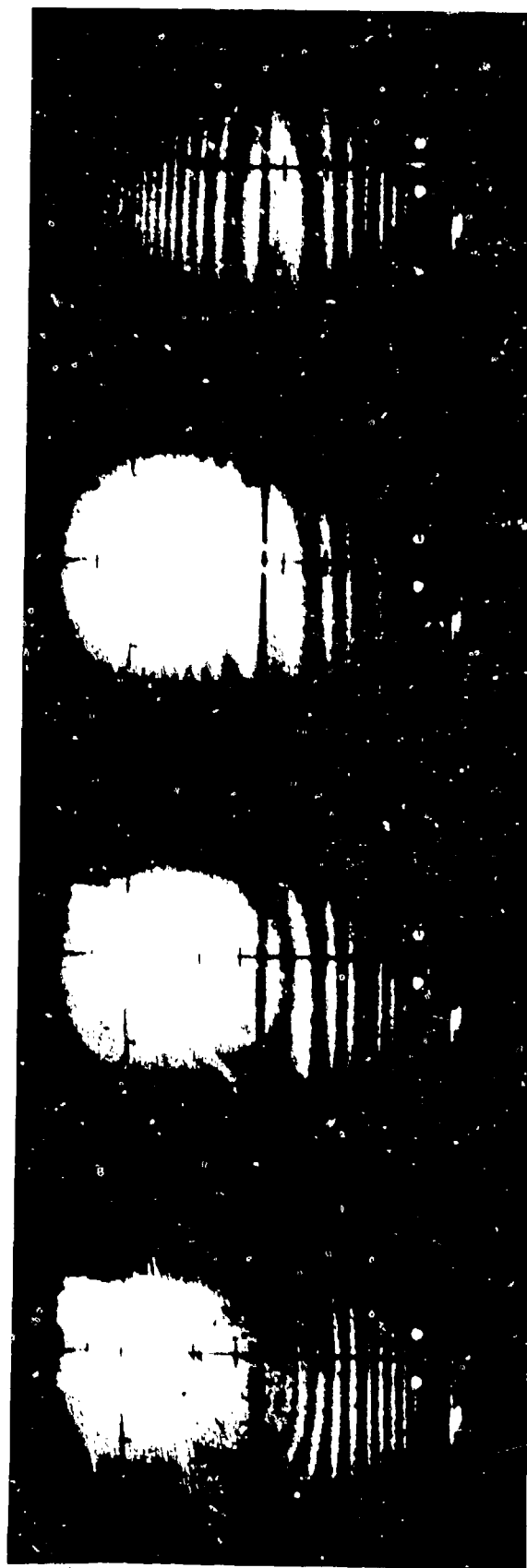


Figure 3.27. Moiré fringe record from Shot FD8. The interframe time was 160  $\mu$ s.

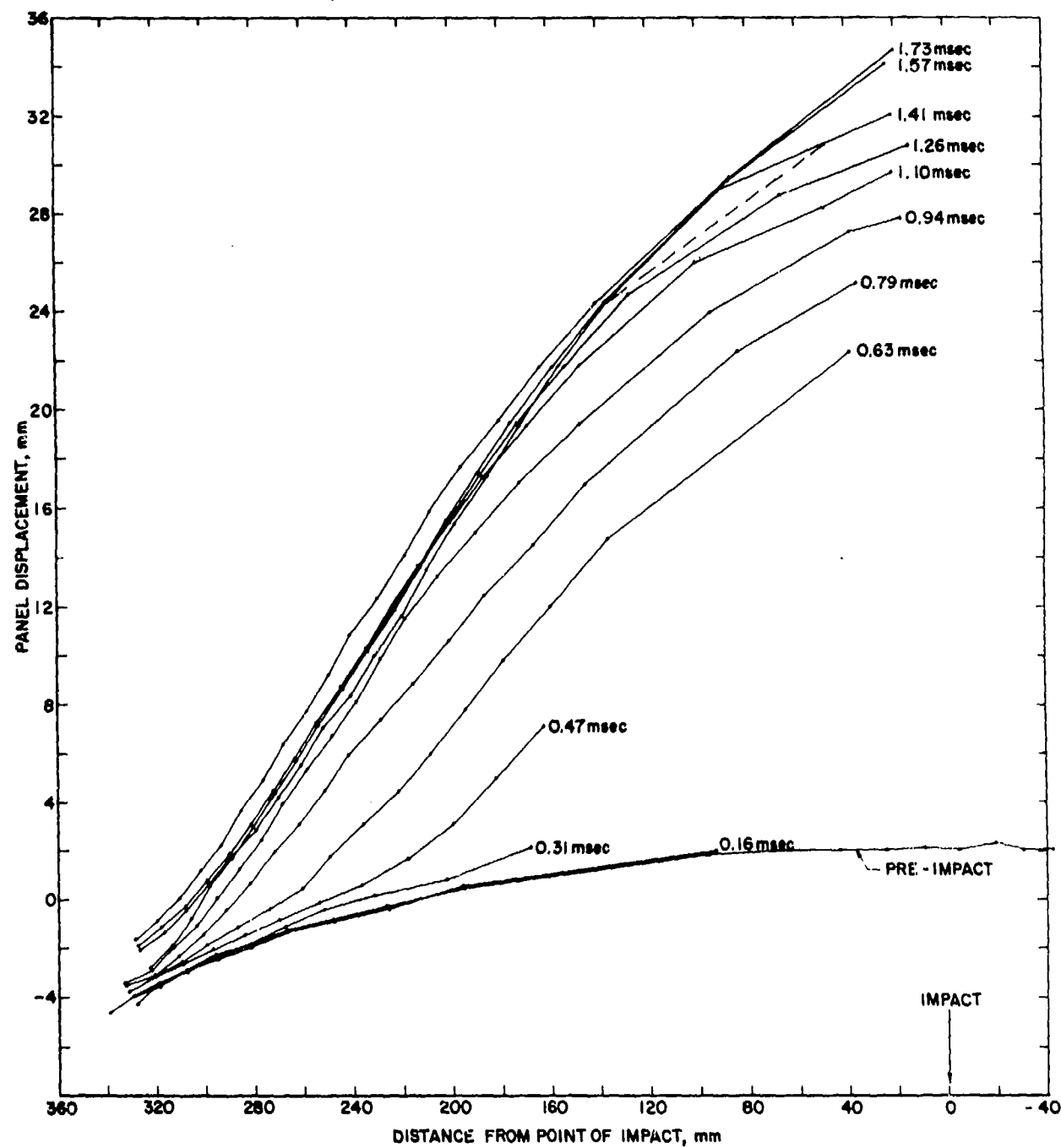


Figure 3.28. Shape of panel in FD8, up to peak displacement.



displacement (with respect to the initial position) was about 35 mm and occurred at about 1.73 ms after impact.

For Shot FD8, the rebound of the panel after peak displacement was analyzed. This analysis was made possible by the computer code developed for fringe data reduction (described in the Appendix). Figure 3.29 shows the shape of the panel at various times between peak deflection and maximum rebound. At maximum rebound (7.5 ms), the deflection of the center of the panel was more than 10 mm below the edges. During the rebound, as well as during the initial outward motion, the fastener region steadily moved outward. At maximum rebound, this region had been displaced almost 7 mm from its initial position. Considerable care was required for proper analysis of the fringe photographs when fastener movement occurred. Figure 3.30 shows the recovery of the panel at various times after rebound. Negligible additional movement at the fasteners occurred during this time.

From these data the time-history of the deflection at various positions can be deduced. This time-history is shown in Figure 3.31 for three positions: 25 mm to the left of impact ( $r = 25$  mm), 170 mm to the left of impact (halfway between the impact and the fasteners), and 280 mm to the left of impact (over the O-ring seal, 18 mm to the right of the fastener center lines). The initial velocity of the panel near the impact point was at least 32 m/s.

The deflection at the fasteners was larger than had been anticipated. The major component of this motion appears to have been the outward movement of the 25-mm-thick aluminum backing plate to which the test panel was bolted. No permanent set appeared in this plate. Therefore, most of the motion must have been caused by a bulk forward motion of the entire tank. Net forward displacement of the tank after the shot was noted in most of the shots. A 10- to 20-mm displacement was typical, and the displacement was greatest when failure does not occur.

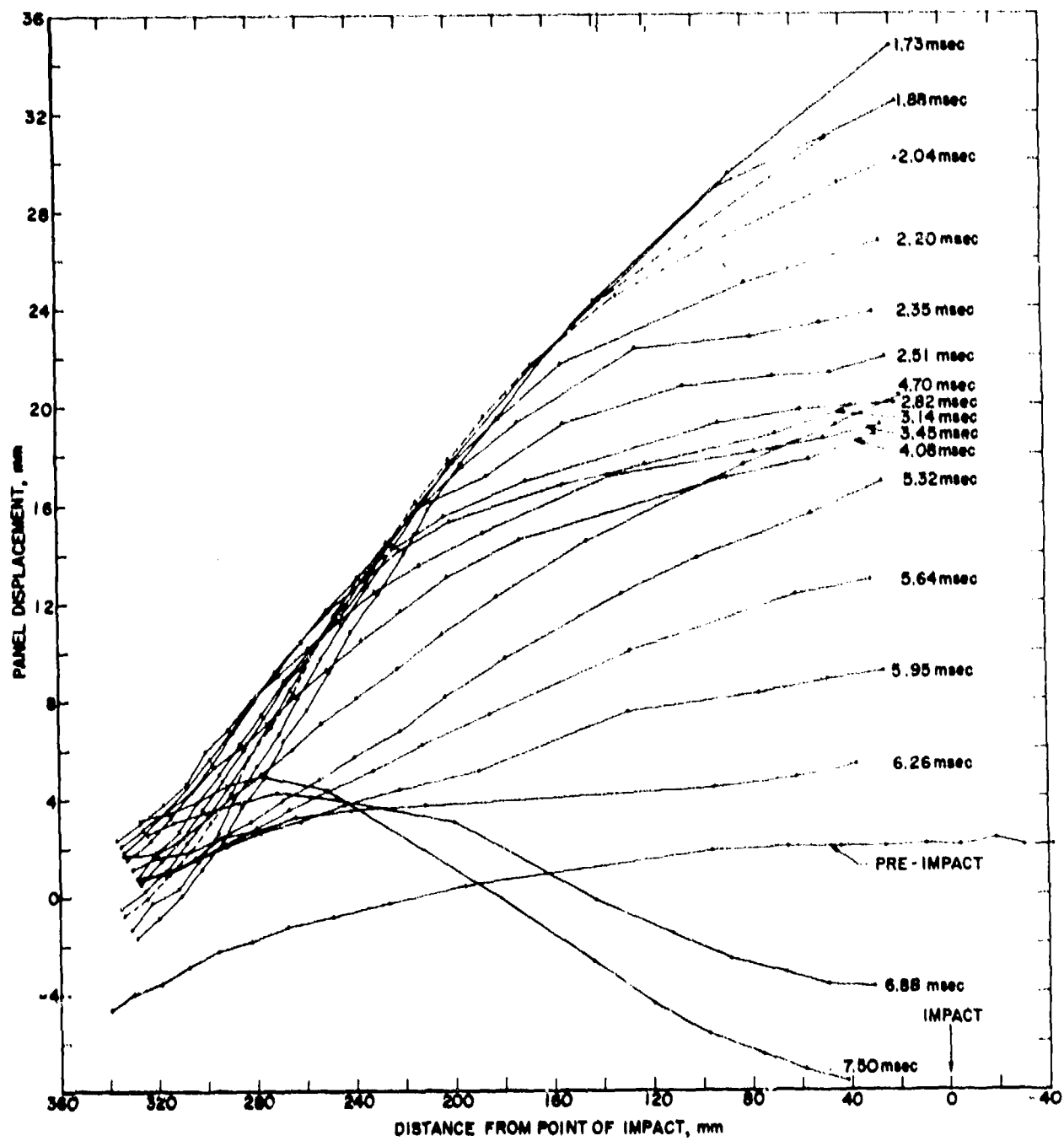


Figure 3.29. Shape of panel in FD8, from peak displacement to maximum rebound.

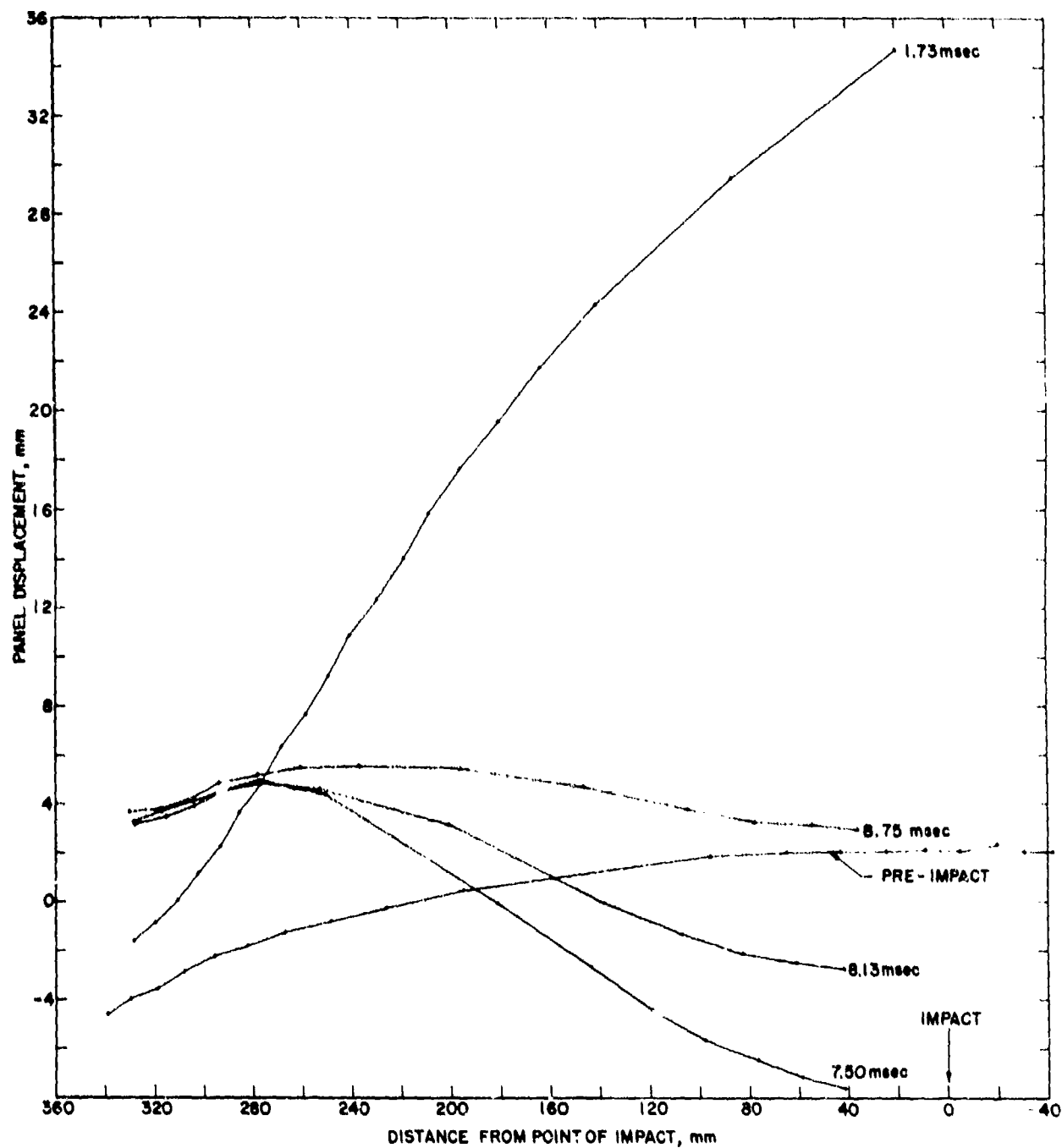


Figure 3.30. Shape of panel in FD8, after maximum rebound.

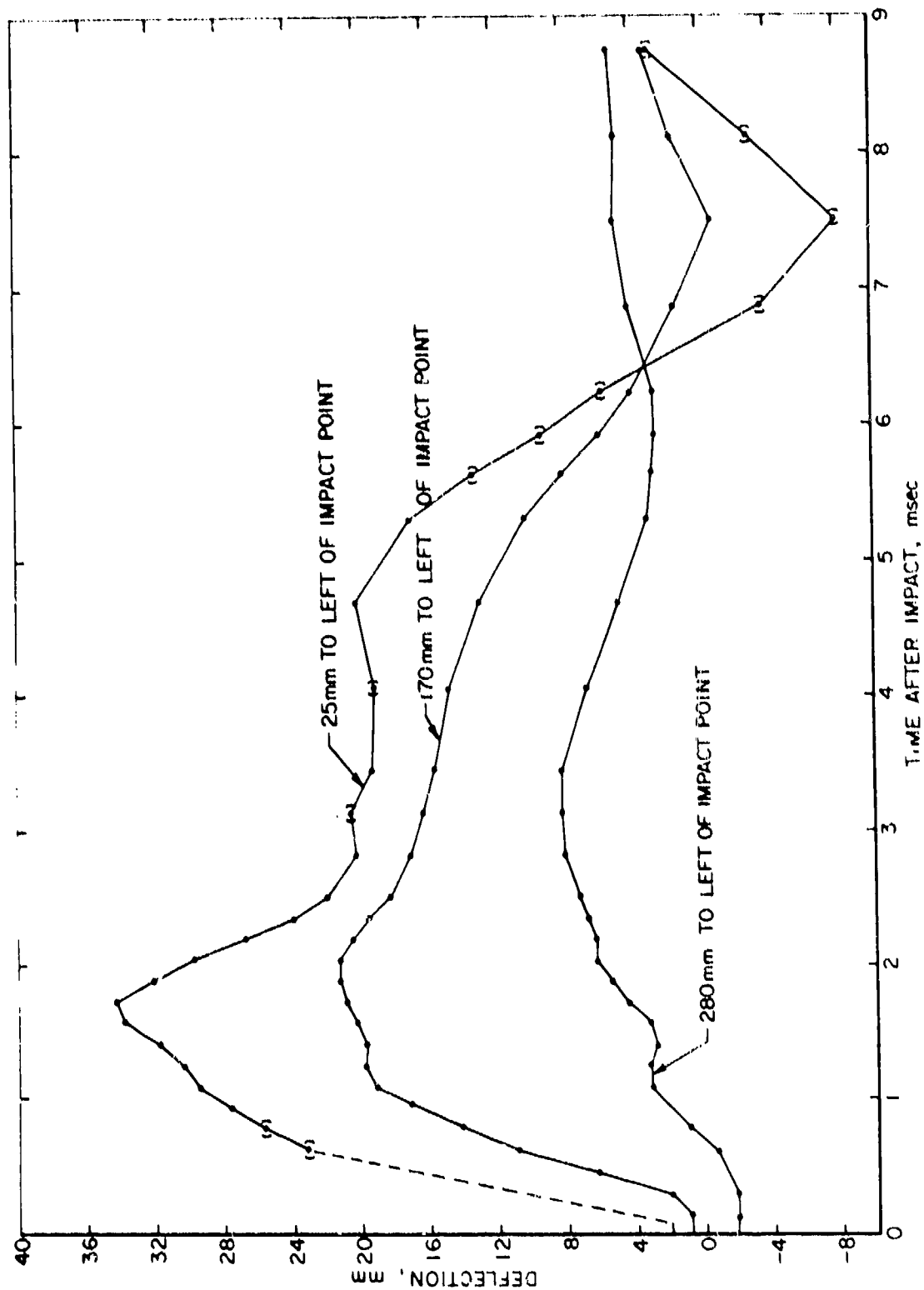


Figure 3.31. Time history of FD8 panel at various positions.

Other causes of panel movement at the fasteners could have been bolt stretching and bulging between the fasteners. The fringe records showed no evidence of bulging between the fasteners. However, some bolt stretch was indicated by the condition of the bolts after the shots and by the fact that a tilt of about five degrees developed in the panel in the region to the left of the O-ring seal. (The initial tilt due to hydrostatic bulge was about two degrees.)

During certain time intervals in Shot FD8, water spurted out the sides of the panel at a velocity of  $\sim 140$  m/s. This leakage could have happened only when the panel had been pushed a sufficient distance beyond the O-ring seal at  $r = 280$  mm. Table XI provides a summary of these observations. The appearance or disappearance of the water leakage correlated roughly with the deflection of the panel at the O-ring (see also Figure 3.31); however, it did not correlate well with the tilt of the panel near the O-ring. This indicated either that the movement of the aluminum base plate was relatively small at these times or else that the times of maximum local displacement coincided with times of maximum local water pressure and that chiefly the water pressure caused the defeat of the O-ring.

The deflection of the panel at the edges necessitated a correction of the data when they were compared to the numerical predictions. For example, the peak displacement in FD8 may be expressed as

- 35 mm, relative to the preshot surface (taking into account the initial hydrostatic bulge).
- 40 mm, relative to the plane defined by the pre-impact position of an unbulged panel.
- 35 mm, relative to an unbulged panel whose edges coincide with the deflected panel at the time of peak deflection.
- 30 mm, relative to a hydrostatically bulged panel whose edges coincide with those of the deflected panel.

TABLE XI. OBSERVATION OF WATER LEAKAGE FROM SIDES OF FD8 PANEL

	Frame No.	Time (ms)	Displacement* (mm)	
			$d_1$	$d_2$
Water Appears	17	2.67	6.2	6.7
Water Stops	29	4.55	6.3	4.3
Water Appears	47	7.37	4.2	1.7
Water Stops	65	10.18	---	---

\* $d_1$  is the displacement at the O-ring, measured from the initial position.

$d_2$  is the displacement at the O-ring, measured from the horizontal line through the instantaneous position of the edge of the panel.

$d_1$  and  $d_2$  are at the time that water started over the O-ring.

### 3.3.2 Displacement Data from Other Configurations

Displacement data available for other configurations are illustrated in Figures 3.32 through 3.38. When using these figures, consult the caption and note whether the displacement is plotted relative to a vertical plane or relative to the pre-impact surface. Additional data for peak displacement in the graphite epoxy panels are given in Table XII. Figure 3.39 illustrates panel motion when failure occurs; movement is a great deal more rapid than in an intact panel.

The uncertainty in displacement for the FD shots was only about  $\pm 0.25$  mm. The uncertainty in the FTC shots was somewhat greater, about  $\pm 1$  mm.

### 3.3.3 Variation in Displacement

Although the data are not conclusive, it appears that for unstiffened panels the only parameter that had a large effect on displacement was panel material. The graphite epoxy panels and the stiffened panels underwent substantially less displacement than the bare aluminum panels. Our data do not

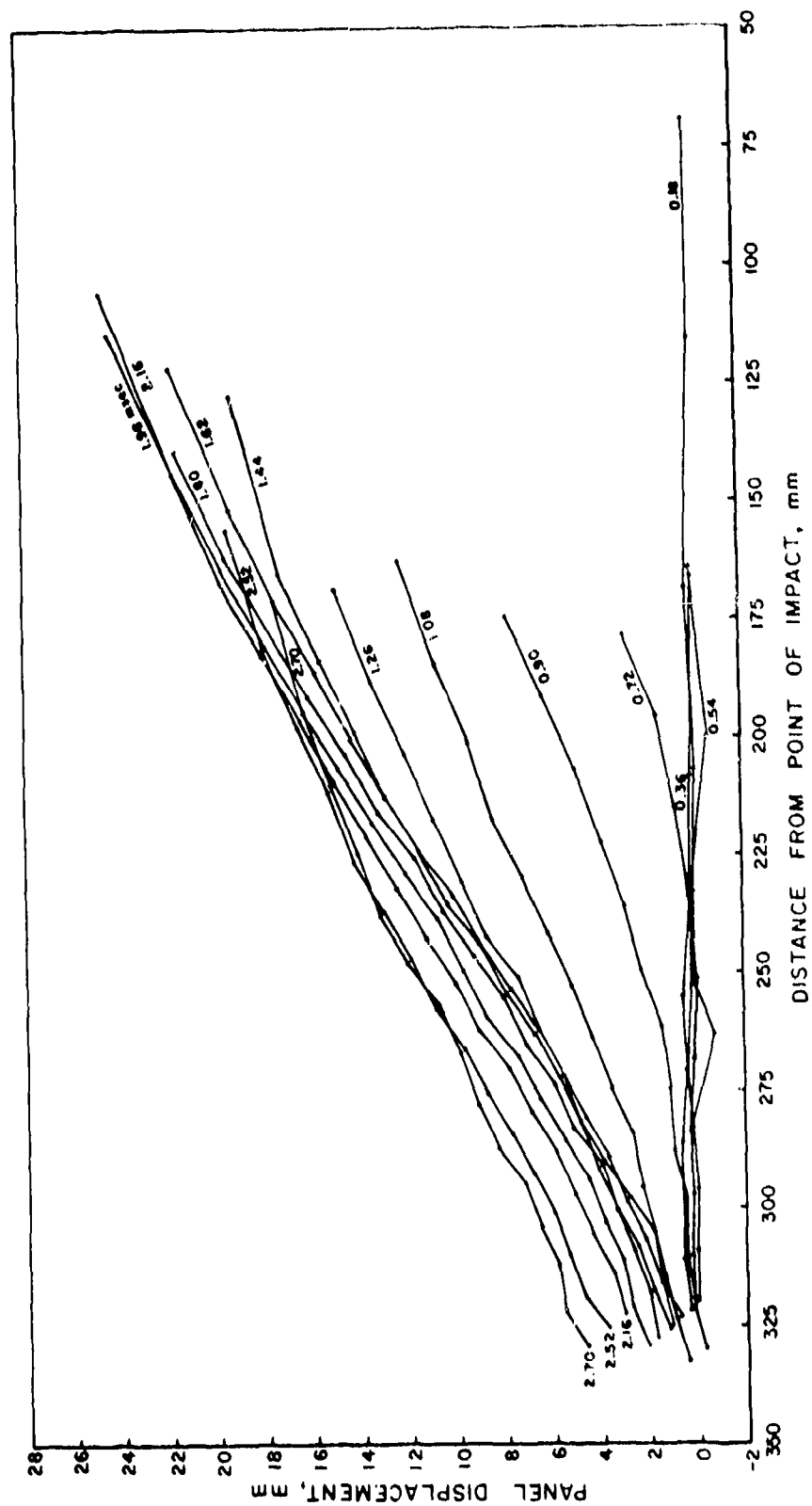


Figure 3.32. Displacement (relative to pre-impact surface) data from Shot FD11 (7LF).

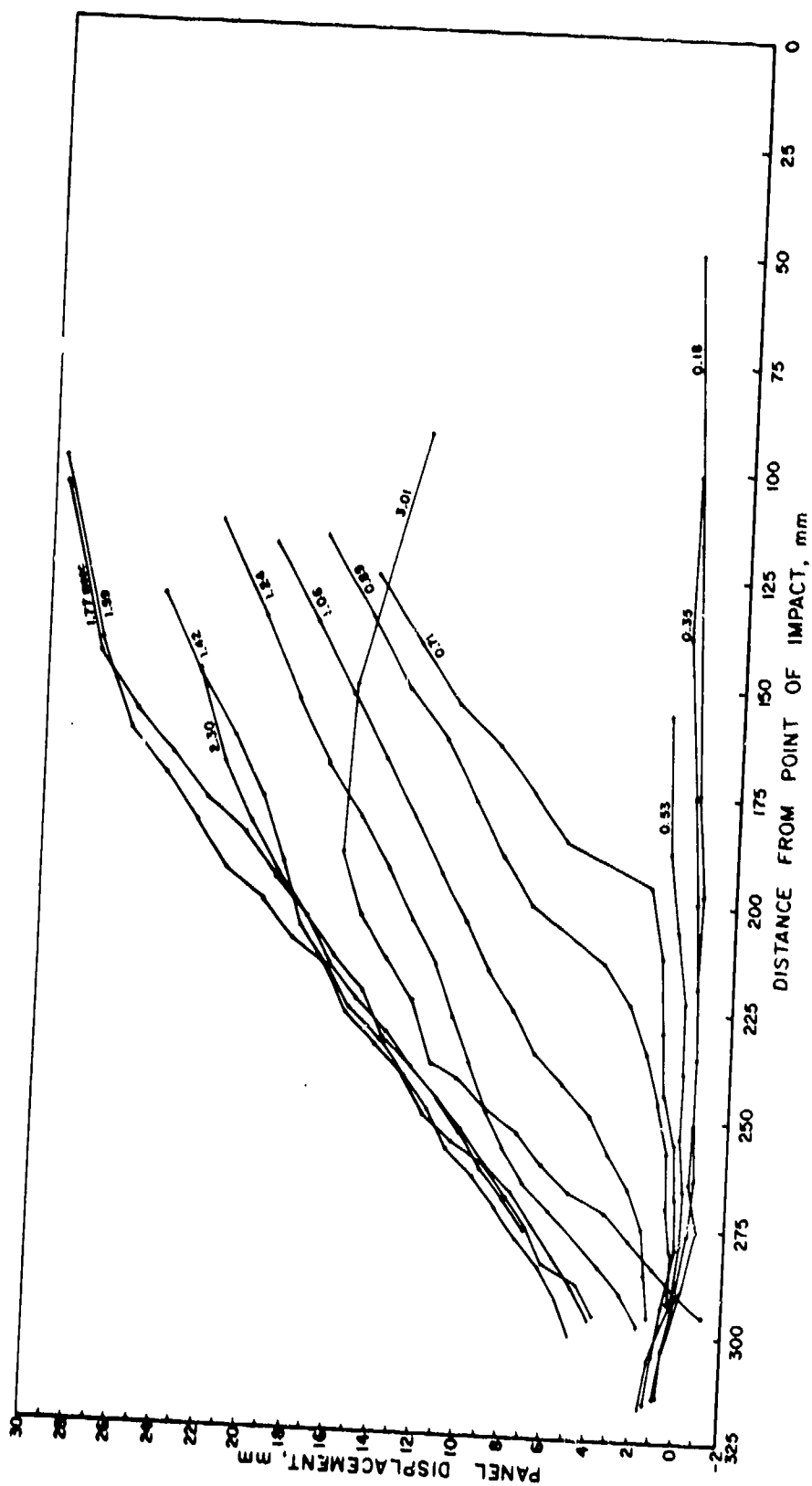


Figure 3.33. Displacement (relative to preimpact surface) data from Shot FD16 (7SF).



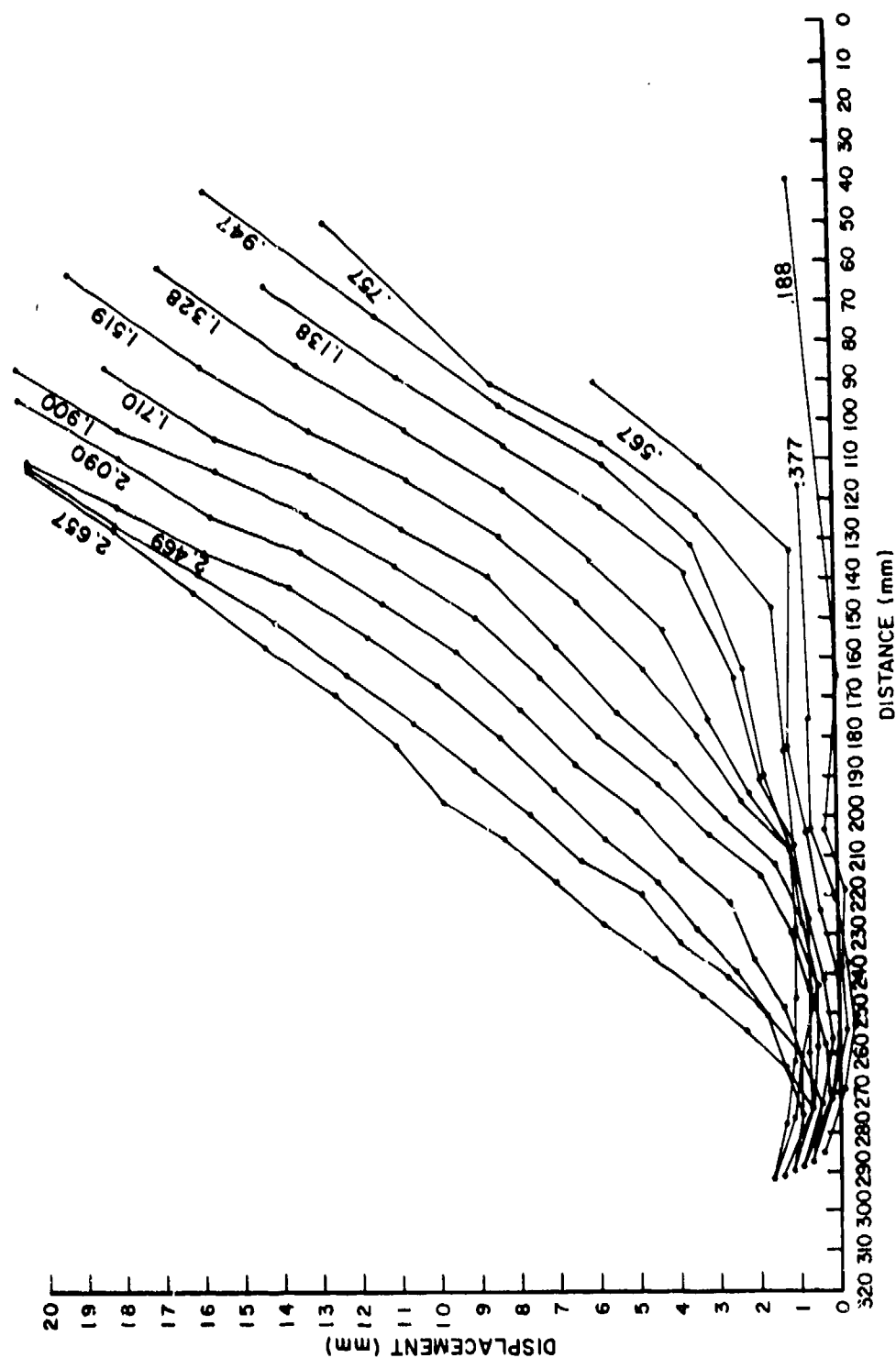


Figure 3.34. Displacement (relative to preimpact surface) data from Shot FD41 (2s1.35)

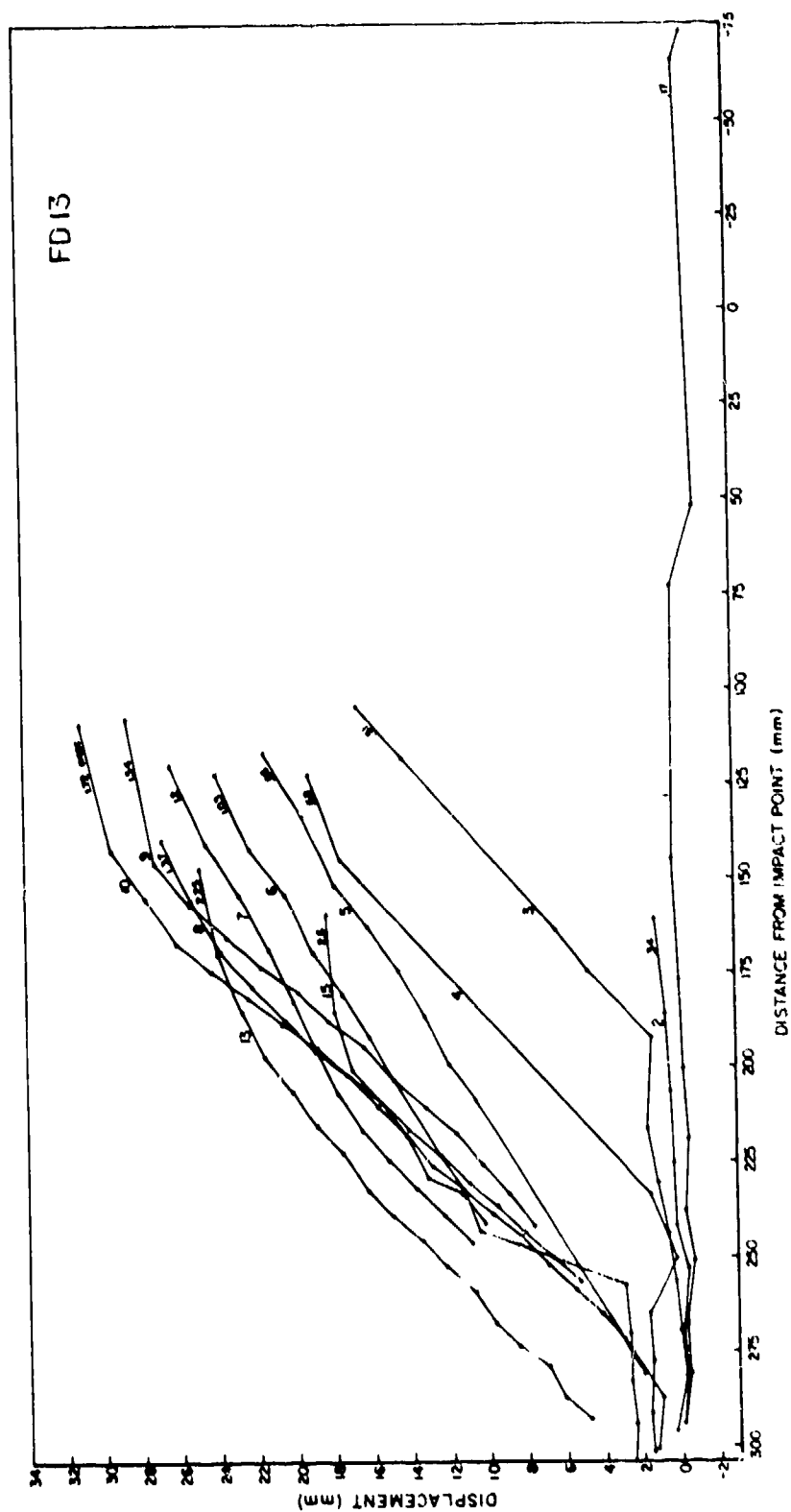


Figure 3.35. Displacement (relative to preimpact surface) from Shot FD13 (7LF1.58).

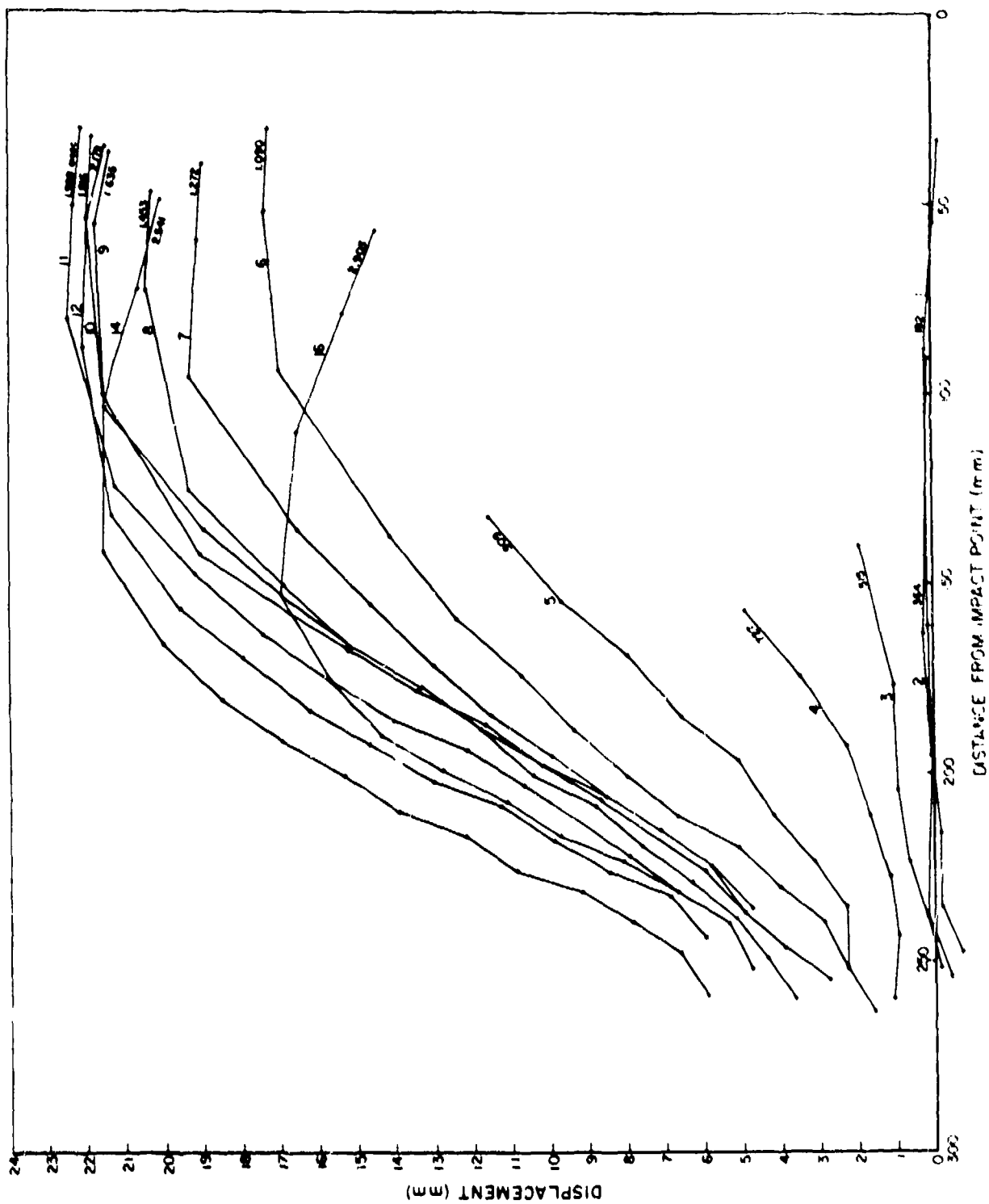


Figure 3.36. Displacement (relative to preimpact surface) from Shot FD26 (GSF1.69).

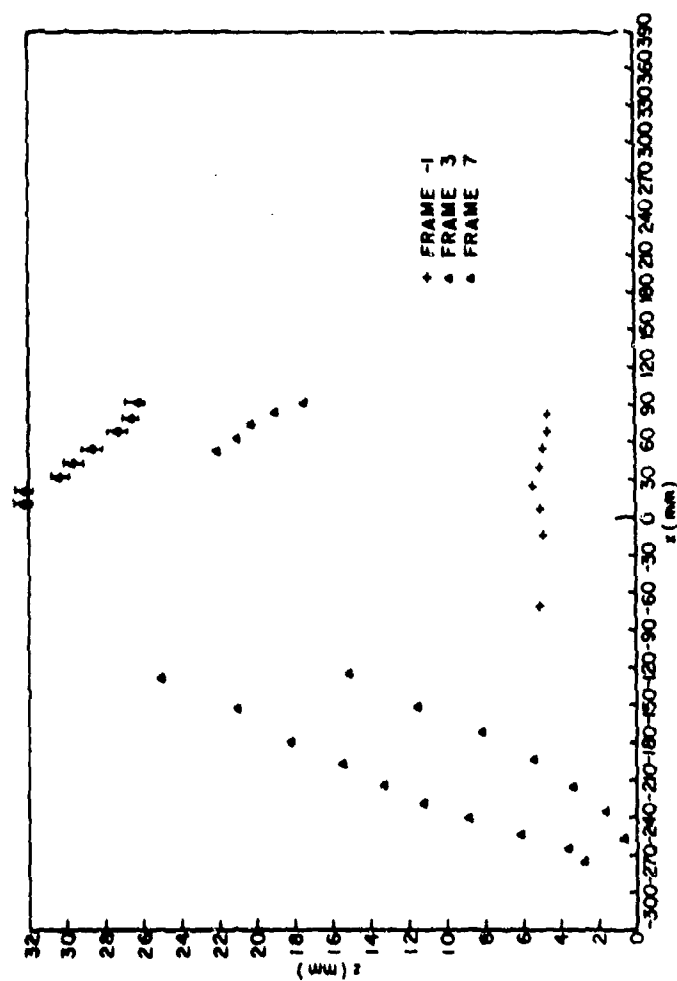


Figure 3.37. Displacement of panel in Shot FTC2 (2S1.55) at selected times before impact (frame-1) 0.645 ms (frame 3) and 1.29 ms (frame 7).

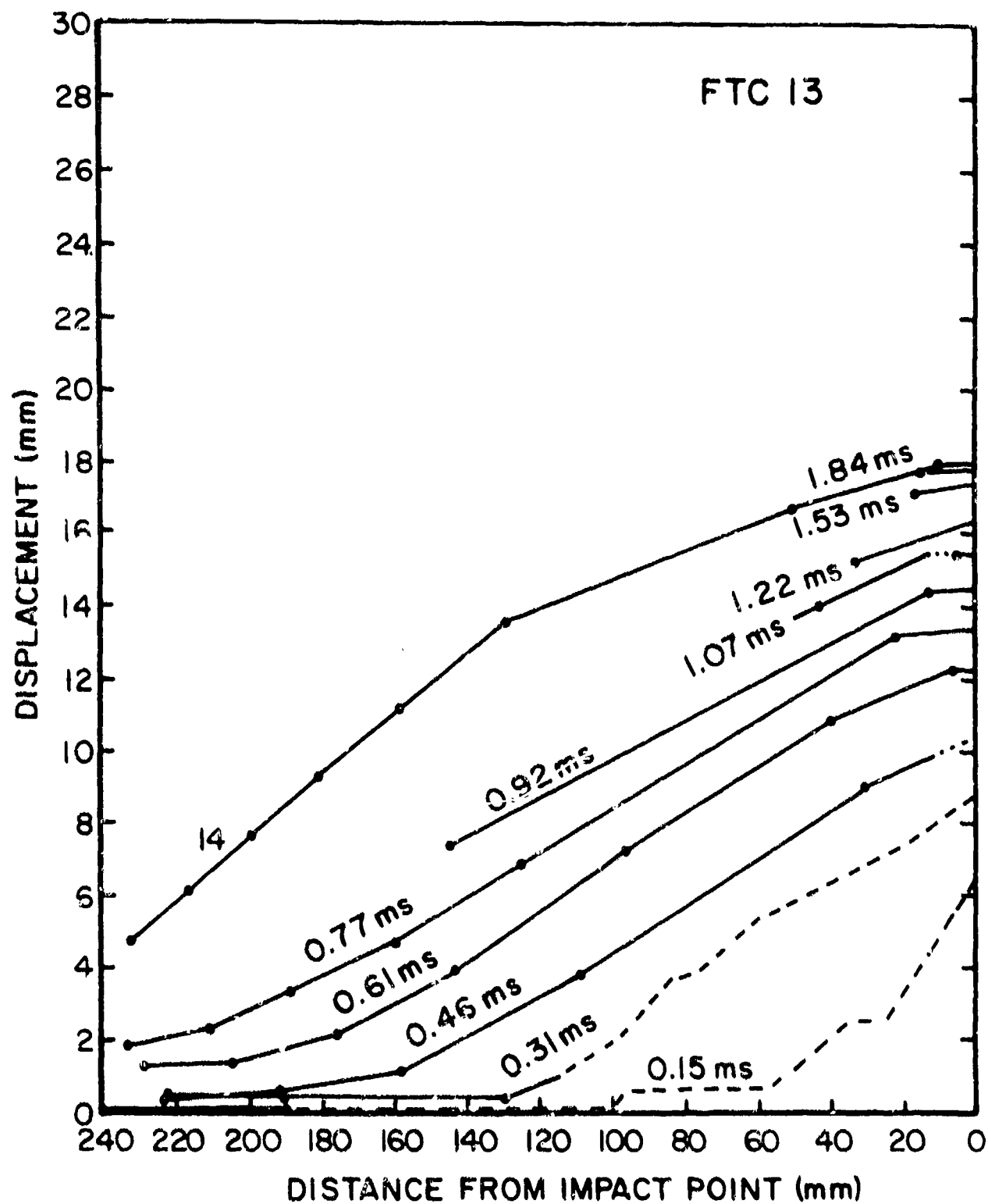


Figure 3.38. Displacement (relative to preimpact surface) from Shot FTC13 (GSF1.18). Numbers indicate frames after impact; average interframe time was 0.153 ms.

TABLE XII. DATA FOR PEAK DISPLACEMENT (RELATIVE TO PREIMPACT SURFACE) IN GRAPHITE EPOXY PANELS. PROJECTILE WAS 5.67-g SPHERE

Shot No.	Panel Thickness (mm)	Foam Thickness (mm)	Impact Velocity (km/s)	Peak Displacement (mm)	Time of Peak Displacement (ms)
FTC4	3.18	0	0.96	12	1.4 - 1.5
FTC6	3.18	0	1.01	16	1.5 - 1.8
FTC8	4.76	0	1.11	22	
FTC9	4.76	0	1.36	22	1.4 - 1.5
FTC11	4.76	11	1.65	21	1.5 - 1.8
FTC12	4.76	11	1.98	26	1.7 - 2.0
FTC13	3.18	11	1.18	17	1.4 - 2.1

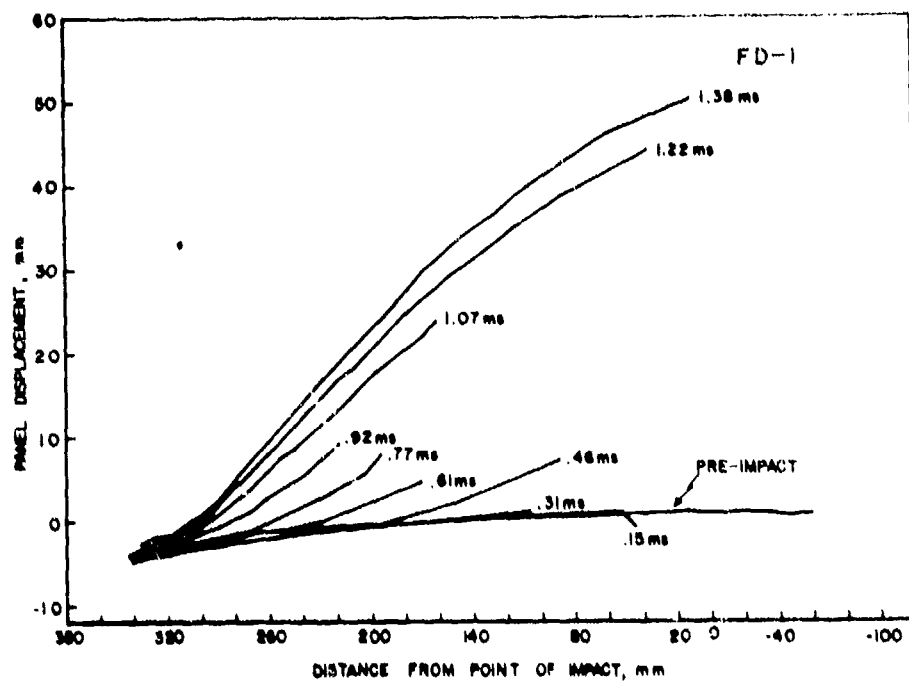


Figure 3.39. Panel displacement along the torn flap in Shot FD1 (see Figure 3.4 for post-shot view).

allow a direct comparison of displacements from two impacts differing only in thickness. The effect of panel thickness and strength may be suggested by a comparison of data from FTC2 (1.6-mm-thick 2024-T3 aluminum) and from FD8 (6.35-mm-thick 7075-T6 aluminum). These shots were fired at similar velocities, although for FTC2 a 5.7-g sphere was used and for FD8 a 11.7-g cube was employed. The displacements in these two shots were almost within experimental error of one another: 33 mm in FTC2 and 31 mm in FD8. The time of peak displacement was only about 200  $\mu$ s later in the thick 7075-T6 panel than in the thin 2024-T3 panels. Although the maximum displacement was less in the graphite epoxy panels, the early-time shapes of these panels were nearly the same as those of aluminum panels, as can be seen from Figure 3.40. Figure 3.41 illustrates the early-time motion of a foam-backed graphite epoxy panel. The initial velocity was  $\sim 40$  m/s.

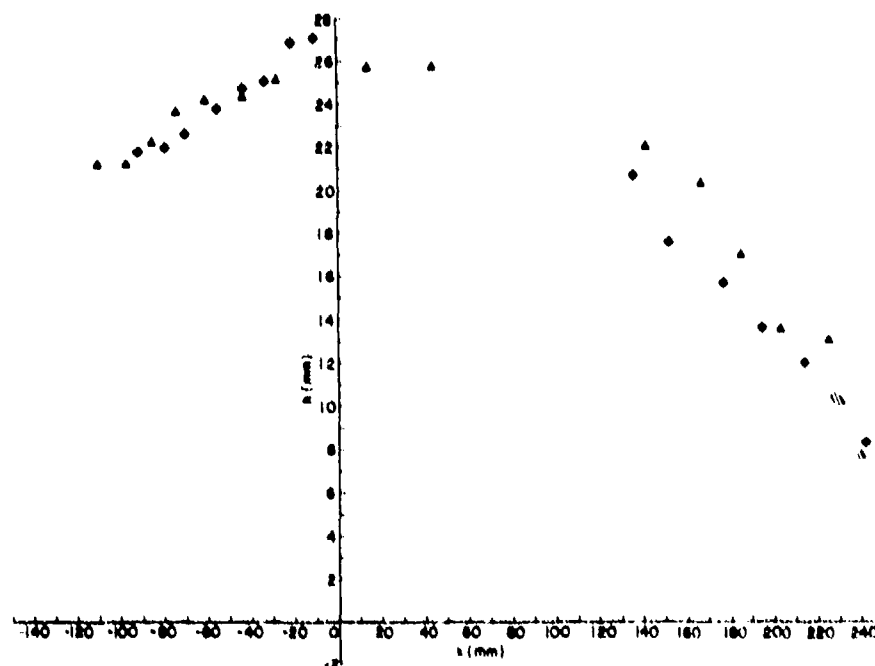


Figure 3.40. Comparison of relative displacement of graphite/epoxy panel of FTC12 at  $1.79 \pm .03$  ms with aluminum panel of FTC2 at  $1.29 \pm .08$  ms.

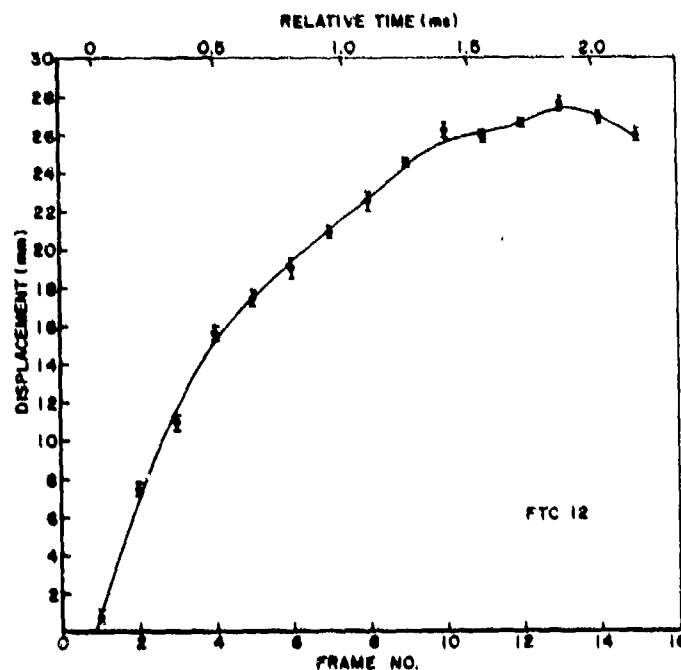


Figure 3.41. Displacement at impact point for Shot FTC12.

Foam had little effect on peak displacement. For example, shots FD8 and FD11 had peak displacements within 7.5 percent of one another at  $r = 125$  mm. However, the foam panel moved slower; it reached its peak at about 1.98 ms, compared to 1.73 ms for the aluminum panel. Similar observations can be made regarding FTC9 and FTC11; the foam did not produce a measurable reduction in peak displacement.

Few data are available for the dependence of peak displacement on impact velocity because high velocities caused the panels to fail. In the GS shots, peak displacement increased 5 mm between  $V = 1.11$  and  $V = 1.36$  km/s. However, the relative insensitivity of displacement with respect to other parameters noted implies that at higher velocities the displacement of aluminum panels would not vary much with velocity.

On Shot FD41, two strain gauges were bonded onto the front of the stiffened panel (configuration 2S1.35). A hoop strain gauge was placed at  $r = 69$  cm and a radial strain gauge, at  $r = 78$  mm. The records are presented in Figure 3.42.



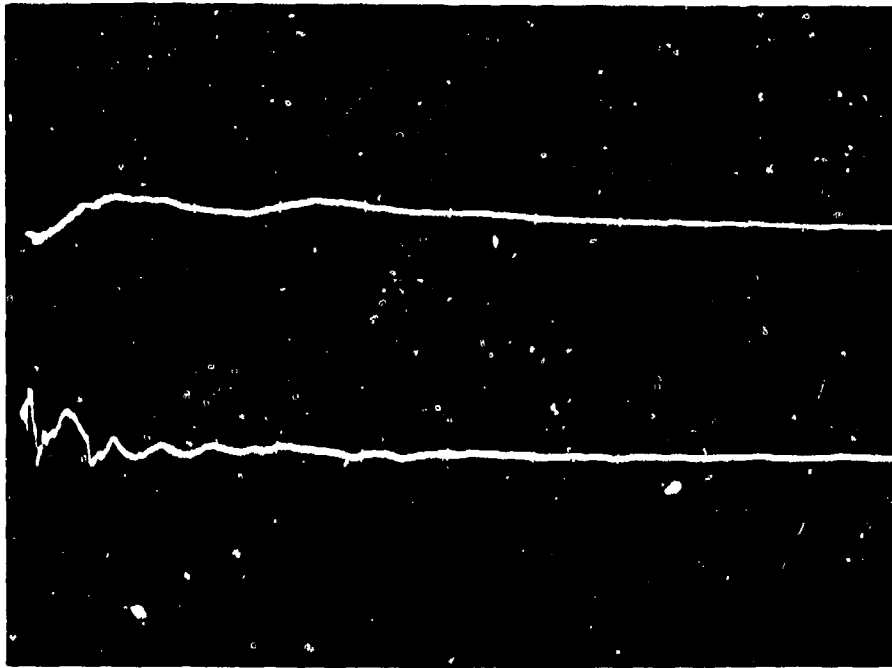


Figure 3.42. Strain record (Shot FD41 (2S)). Top trace, hoop strain; bottom trace, radial strain. Sweep is 0.5 ms/div. Scale is 1 percent strain/div.

The hoop strain record shows an arrival at  $\sim 50 \mu\text{s}$  and attainment of maximum strain at about 0.5 ms. The maximum hoop strain was about 0.5 percent. Strain maximum occurred later than the time of maximum displacement (shown in Figure 3.43), which was at 2.7 ms. There was a plateau in hoop strain at  $\epsilon_0 = 0.4$  percent at 0.22 ms after impact. The maximum strain expected is of order 2 (maximum deflection/panel radius)<sup>2</sup>  $\sim 2$  percent. We conclude that a bond failure probably occurred, and measured  $\epsilon_0$  is unreliable. The radial strain gauge shows ringing, as has been observed by other investigators who have strain gauged panels.<sup>1</sup> The radial strain transient occurred too fast to be resolved on the oscillograph; the final radial strain, achieved in about 0.5 ms, was also about 0.4 percent.

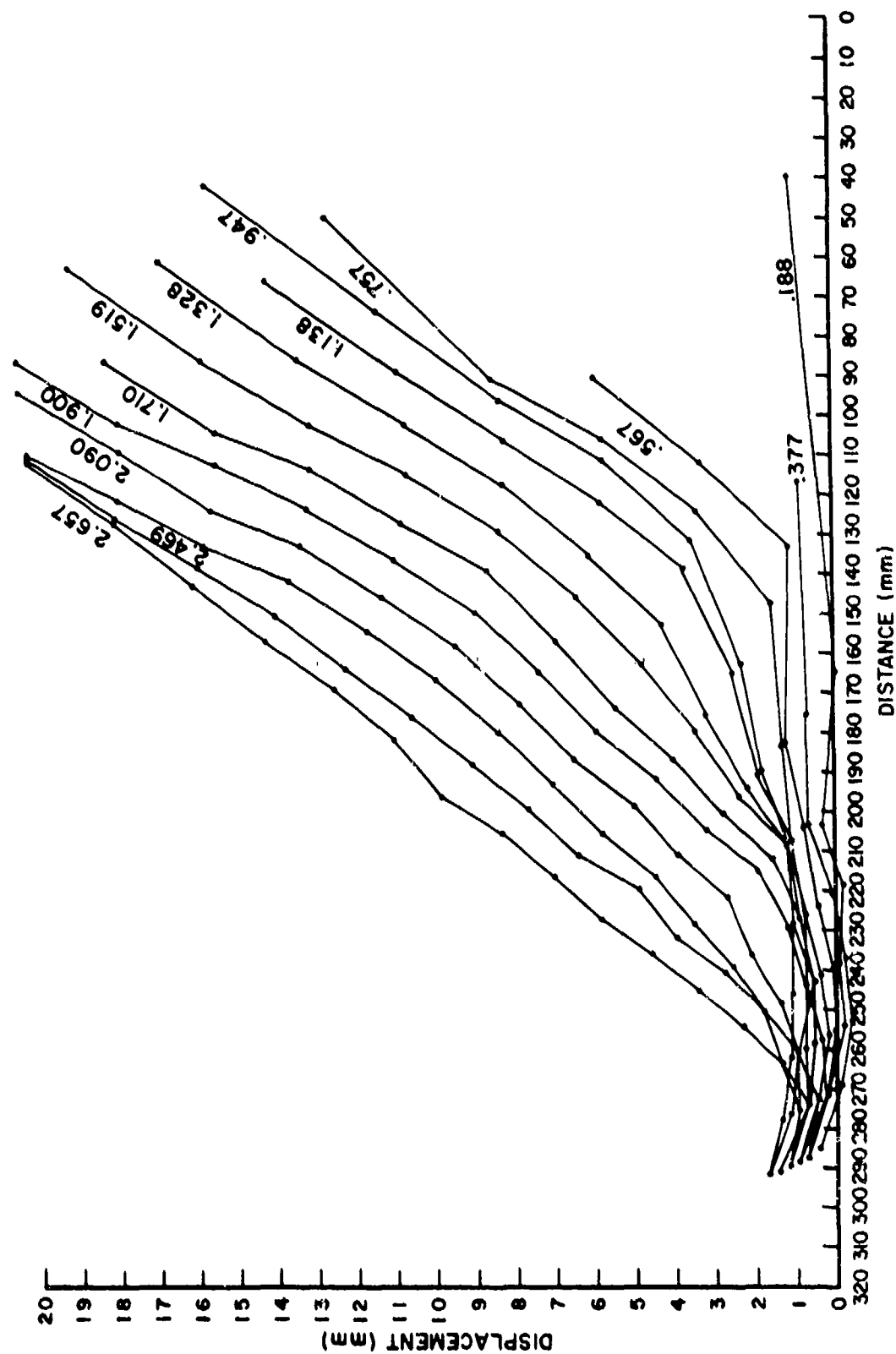


Figure 3.43. Displacement (relative to preimpact surface) at various times for Shot FD41 (2S1.35).

#### SECTION IV

##### SUMMARY OF ANALYTICAL RESULTS

Finite difference and finite element analyses of hydrodynamic ram phenomena were performed by CRT under subcontract to the University. Details of the numerical approach and results are described in Part II of this report. In this section we summarize and comment upon what we believe to be the most important features of the CRT numerical analysis.

The goals of the numerical effort were as follows:

- (A) Refine techniques for calculating entrance-panel loading functions, using the CRALE finite-difference code.
- (B) Refine techniques for calculating entrance-panel structural response, using the NONSAP finite-element code.
- (C) Use the results of A to generate design data for entrance-panel loads.
- (D) Use the results of A through C to refine techniques of predicting failure thresholds and to apply prediction techniques to generate design data for failure of panels penetrated by threats of interest.
- (E) Use the results of A and C to develop design data for entrance-panel displacement.

We originally intended to generate design data using numerical techniques almost exclusively. The main purpose of the experiments was to provide validation of the numerical techniques. Specifically, we felt that code credibility could best be established by demonstrating the code's ability to predict failure thresholds, pressure profiles, and displacement

histories. These quantities were measured for key configurations, and CRT was not informed of experimental results until after predictions had been submitted to the University.

The success of the numerical approaches was mixed. Tasks A, B, and C were satisfactorily accomplished. Task D was not successful; however, the results of Task D were very valuable in interpolating and in extrapolating the limited experimental data set to provide design data for failure thresholds. Task E was partially successful; however, at this time it is still more accurate and convenient to base design data on the experimentally measured displacements.

#### 4.1 RESULTS OF PRESSURE PREDICTIONS

The analytical predictions of pressure were based on an axially-symmetric finite-difference representation of the entrance panel and water. The projectile was modelled as a rigid sphere. The arrival times and general form of the predicted pressure pulses were satisfactory. In the initial calculations, the peak pressures were much too low. Figure 4.1 illustrates a typical comparison of the numerical and experimental results. The calculated peak pressures were often a factor of two below the experimental data. However, the calculated impulse values were reasonably accurate (see below). In order to provide loading functions for finite-element calculations were approximated by more convenient and simple analytical models. The model was fit to the pressure at  $z = 6$  mm. In the model, pressure is a function of time,  $t$ ; and radius,  $r$ , given by a triangular shaped function that arrived in time,  $T_1$ , peaked at  $T_2$ , and decayed exponentially beyond  $T_3$  with a decay constant equal to  $T_4$  as shown in Figure 4.2. The maximum pressure was given by

$$P_m = 3.0 D^2 W^{.25} V_R^{-(2+.4W)} \quad (3)$$

where  $P_m$  is in kbar (and the other units are cm and  $\mu s$ ) and the times were given by

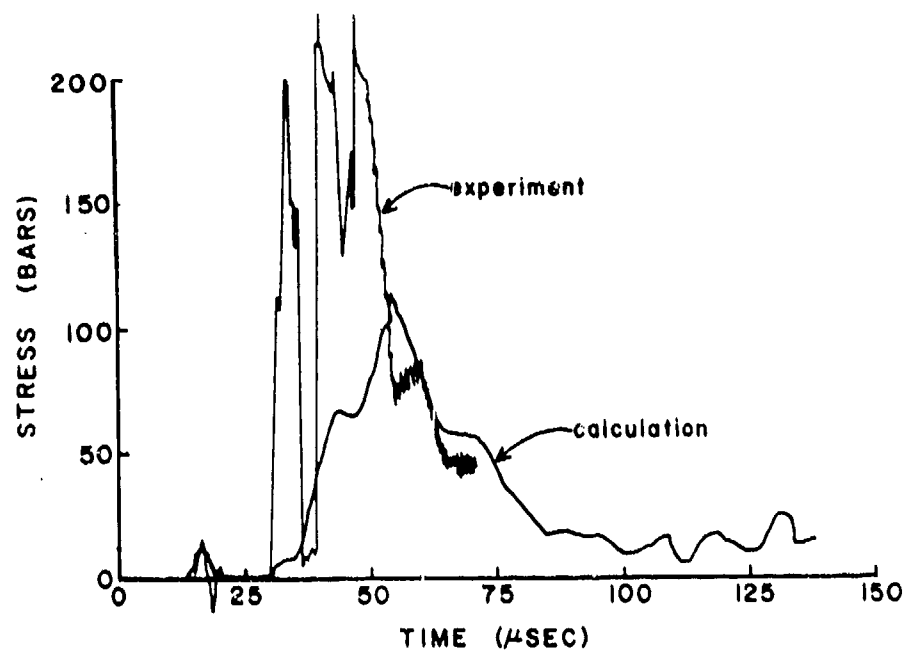


Figure 4.1. Comparison of measured and predicted pressure profile for the  $r = 75$  mm pressure station from Shot FD8.

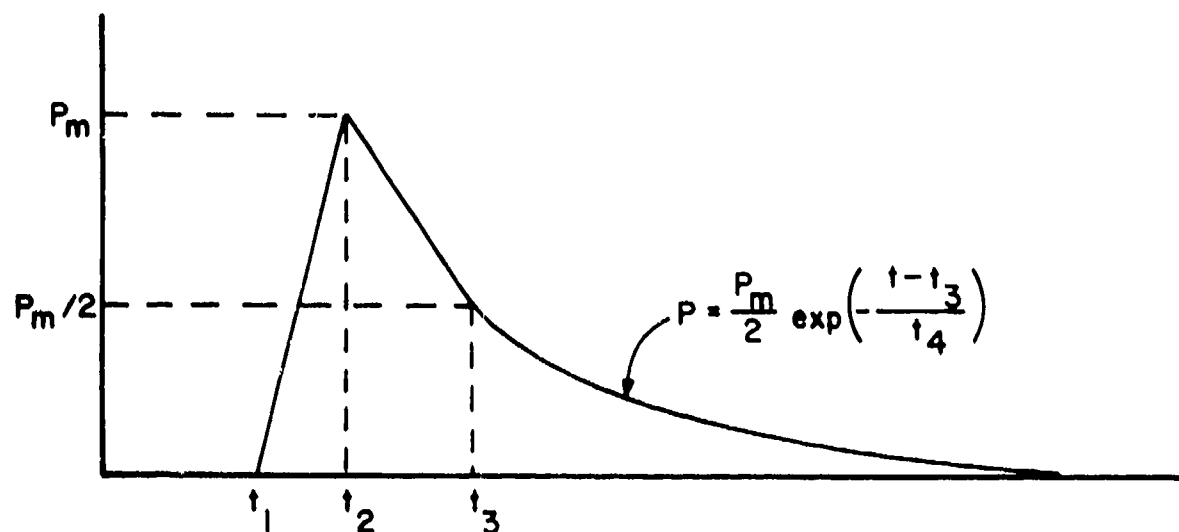


Figure 4.2. Form of loading pressure in analytic model.

$$t_1 = 5 (r - D/2) \quad (4)$$

$$t_2 = (6.4 + 2.1W) (r - D/2) \quad (5)$$

$$t_3 = t_2 + 12.6W \quad (6)$$

$$t_4 = \frac{2}{P_m} [I_{\max} - I (t < t_3)] \quad (7)$$

$$I (t < t_3) = 0.5 P_m [t_2 - t_1 + 1.5 (t_3 - t_2)] \quad (8)$$

where  $I_{\max}$  is the total impulse, in bar-s, given by

$$I_{\max} = 0.0165 D^{.8} W^{.45} V^2 e^{-.365R} \quad (9)$$

The function described by equations (3) through (9) was compared with the experimental data. Figures 4.3 and 4.4 show typical comparisons for thick and thin panels. The numerical pulse arrival times durations, and impulses are in satisfactory agreement with the data.

The data are compared with the predicted impulses in Figure 4.5. The 7S1.35 configuration was chosen as a base, and data from other configurations that differed from 7S1.35 in only one parameter are "mapped" using the scaling implied in Equation 9. Table XIII explains the symbols used in this and subsequent figures. The solid line in the figure shows the loading function used by CRT to derive the structural response model (Equation 9). The dashed line illustrates constant impulse beyond  $r = 10$  cm, as assumed in some of the finite element calculations.

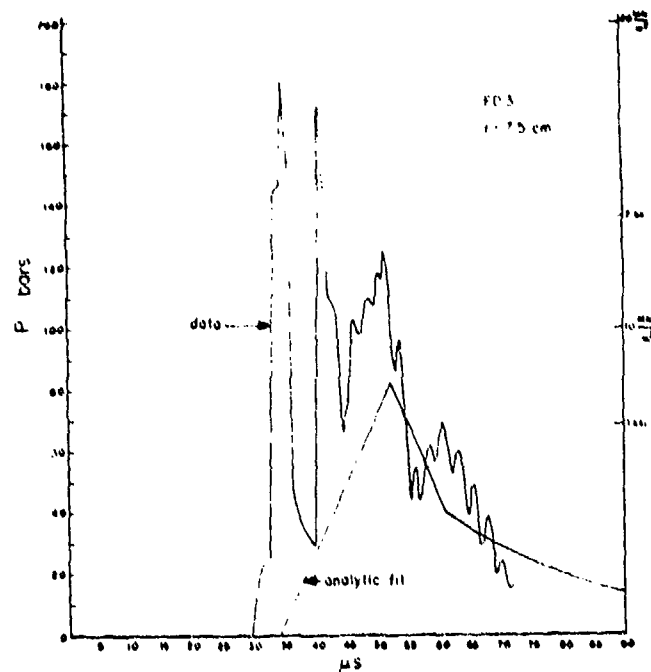


Figure 4.3. Comparison of experimental and model pressure histories for FD3, 7L1.42, at  $r = 7.5$  cm.

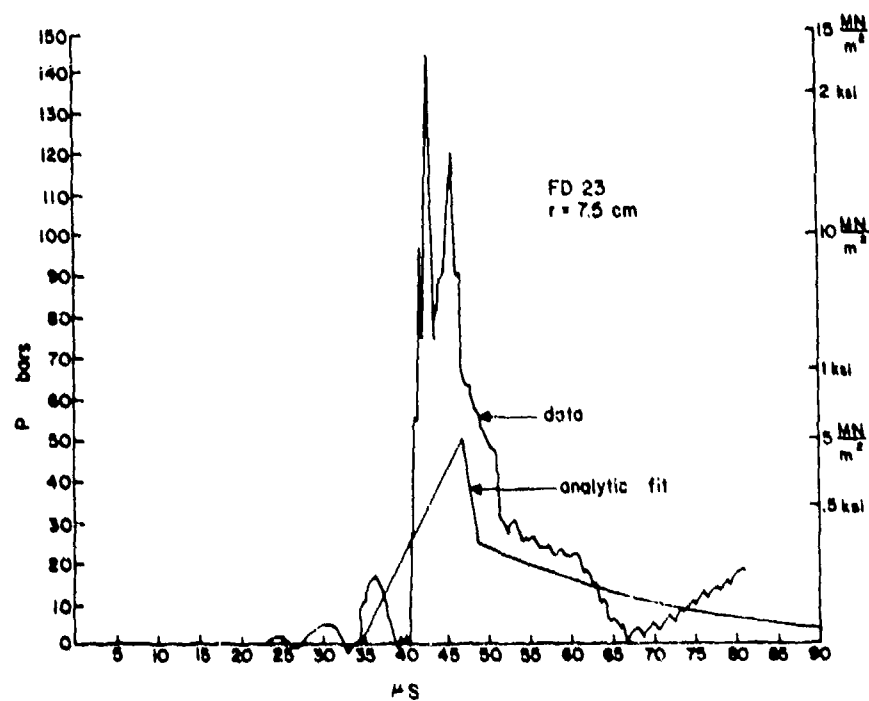


Figure 4.4. Comparison of experimental and model pressure histories for FD23, 7S1.35 at  $r = 7.5$  cm.

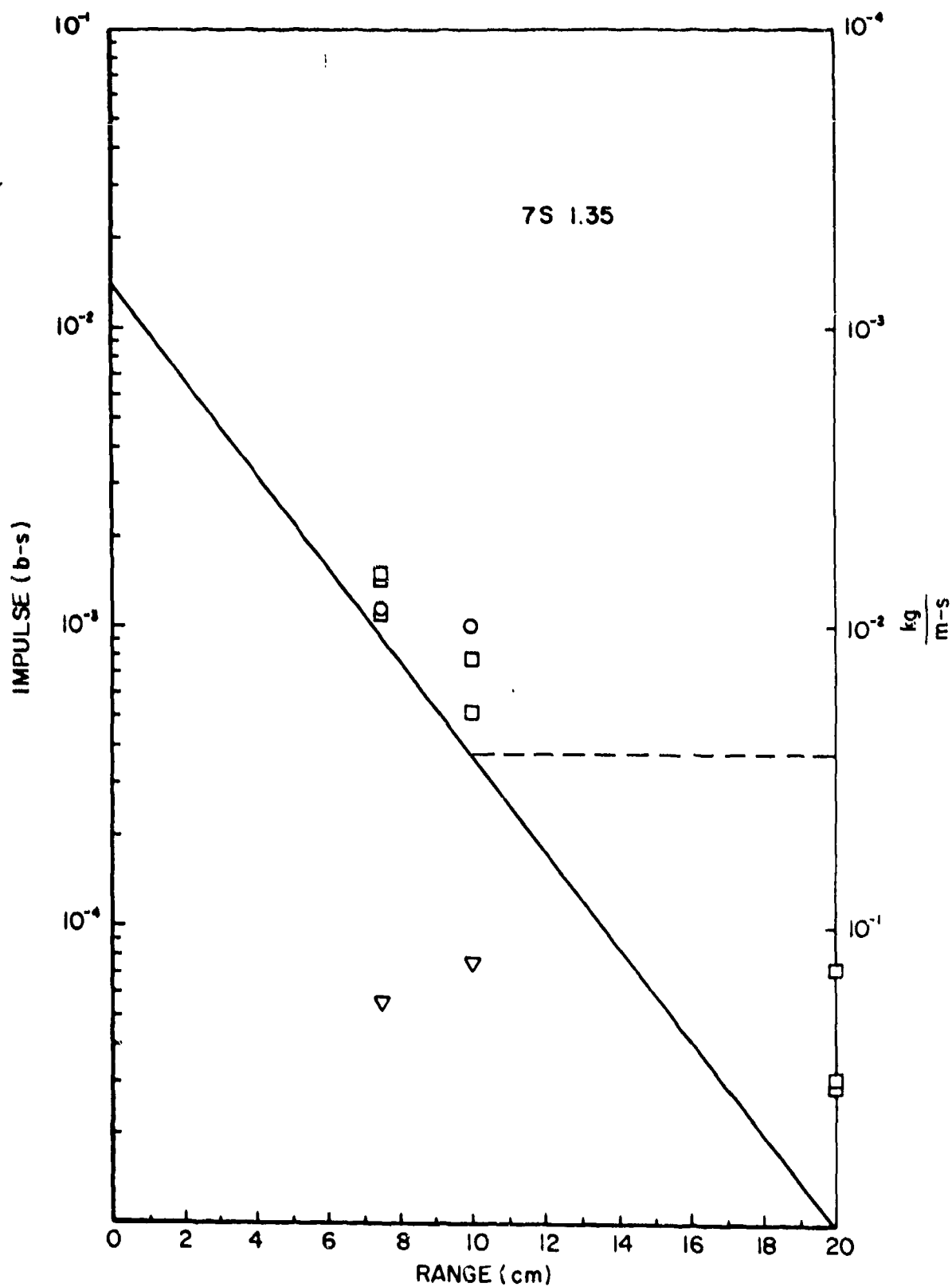


Figure 4.5. Comparison of analytical model (Equation 9) and experimental data for impulse in the 7S1.35 configuration. Note: See Table XIII for explanation of symbols.



TABLE XIII. EXPLANATION OF SYMBOLS USED IN FIGURES 4.5, 4.7, AND 4.8

- Datum from shot that was base configuration
- Datum from shot that differs from base only in impact velocity
- △ Datum from shot that differs from base only in projectile size
- ▽ Datum from shot that differs from base only in panel areal density
- \* Numerical value from finite difference calculation

The analytic model falls close to the measured values (being low by a factor of less than two at 7.5 to 10 cm). At 30 cm the data fall between the two models used by CRT. The impact velocity scaling of the model ( $v^2$ ) seems to fit the data satisfactorily; however, the panel thickness scaling seems to use an exponent that is too high. When the impulse measured in a thick panel shot (in this case Shot FD19, 7Sl.95) was scaled to a thin panel configuration, the results were much lower than those actually observed in thin panel shots. Better scaling would be achieved with an exponent of -1. When the 7L data were compared with Equation 9, they were found to fall below the prediction by a factor of two or three. Perhaps as much as half of this discrepancy may be due to the truncation of the experimental record due to the presence of noise. Thus, the model overpredicts impulse by a factor 1.5 to 2. For many applications this is unacceptable. Thus, an improved model was also developed, as discussed below.

The data do not justify the assumption that impulse is constant beyond 8 cm.

After the first model was formulated, CRT completed additional calculations for other configurations and repeated some

earlier ones with finer zoning. CRT also analyzed the variation of pressure pulses with distance behind the target panel (z). Consideration of these calculations and additional experimental data led CRT to revise the analytic loading model. The revised model is formulated for  $z = 1$  mm. In this model, pressure rose linearly to a maximum value,  $P_m$  and then decayed with a time constant  $t_3$ . The model is defined by the following equations

$$P_m = 3.68 \delta V (R'/R_p)^{-\alpha} \quad (10)$$

$$\alpha = 1.15 + 0.6 \exp (-0.72 \rho_t W) \quad (11)$$

$$I = 0.03 \delta V \exp (-\beta R'/R_p) \quad (12)$$

$$\beta = 0.185 + 0.35 \exp (-0.63 \rho_t W) \quad (13)$$

$$t_2 = W/V + 6.25 (R - R_p) \quad (14)$$

$$t_1 = t_2 - (2W/0.63 + 1) \quad (15)$$

$$t_3 = \min \left\{ \frac{I}{P_m} - 0.5 (t_2 - t_1) \right. \quad (16)$$

$$\delta = \sqrt{R \frac{1}{r}} \quad (17)$$

$$R' = \begin{cases} r & r \leq 10 \\ 10 & r > 10 \end{cases} \quad (18)$$

The units used in these equations are centimeters for length,  $\text{g/cm}^3$  for density, kbar for pressure, and bar-seconds for impulse. (Conversion factors are provided in the Preface.) The behavior of Equations 10 - 18 is relatively simple. Impulse decreases exponentially with range for  $r < 10$  cm. The pressure

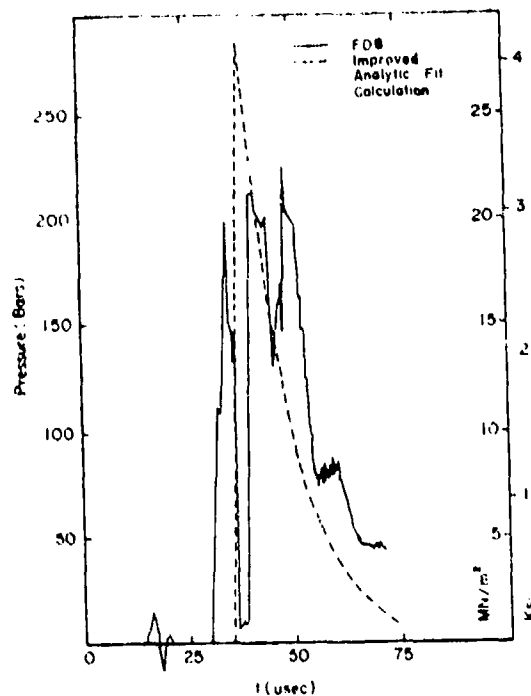


Figure 4.6. Comparison of experimental data from FD8 (7L1.63) at  $r=75$  mm with improved analytic fit and with calculation.

decays approximately as  $r^{-1}$  as would be expected from a point source in an infinite medium: however, the decay constant is a function of panel areal density, since that parameter affects pressure relief. Beyond  $r = 10$  cm, pressure and impulse decay as  $r^{-1/2}$  (as they do from a cylindrical source in an infinite medium). The transition in decay rates was justified from observations of the pressure field in the water as a function of time. We inferred that beyond  $r=10$  cm the initial shock wave was no longer responsible for peak pressure.

Part II of this report contains a thorough comparison of the model defined in Equations 10 - 18 with numerical data from eight different runs. The model adequately described the results of the individual calculations. Fit to experimental data was also improved, as illustrated in Figure 4.6. Small discrepancies can be explained by the fact that the numerical data are computed for  $z = 6$  cm (at the position of the transducer), but the analytic model is formulated for  $z = 1$  mm.

Since panels respond as if loaded impulsively, impulse is the most critical parameter for judging numerical models. Predicted and observed impulses are compared in Figures 4.7 and 4.8. Again, the notation of Table XIII applies. Equation 12 is graphed as a solid line, and the dashed line shows the approximate corrections for translation to  $z = 6$  mm. For both thick and thin panels, the model impulses were about one half the measured ones at  $r = 7.5$  and 10 cm. At 20 cm the model overpredicted impulse by a factor of about two. The scaling relationships implied by Equation 12 were used to prepare these figures. The linear velocity scaling of Equation 12 worked slightly better than the quadratic velocity scaling of Equation 9. The scaling for projectile size (in Figure 4.7) was rather good. However, the areal density scaling in Figure 4.8 was still inaccurate.

The numerical results reveal much of the structure of the pressure field behind the impacted panels. The effect of panel thickness on pressure is particularly interesting. Both the calculations and the experiments show that thick panels give rise to multi-peaked pressure pulses. As explained in Part II of this report, the calculations show how later pulses are caused by the coupling of pressure waves in the water with stress waves in the plate. Energy in the water seems to couple to a wave in the panel and essentially "tunnels" through the aluminum to reappear ahead of what was initially the wavefront in the water. This leads to a region around  $r = 10$  cm in which impulse becomes nearly independent of range. The anomalous-appearing experimental data in Figure 4.7, which shows larger impulses at  $r = 10$  cm than at  $r = 7.5$  cm, were probably caused by this effect.

The modelling of perforation in the finite element code is relatively crude. The aluminum is only three zones thick and peels away from the impact site. As a consequence, the numerical calculations probably underestimate the strength of the initial shock waves in the water. These waves were caused

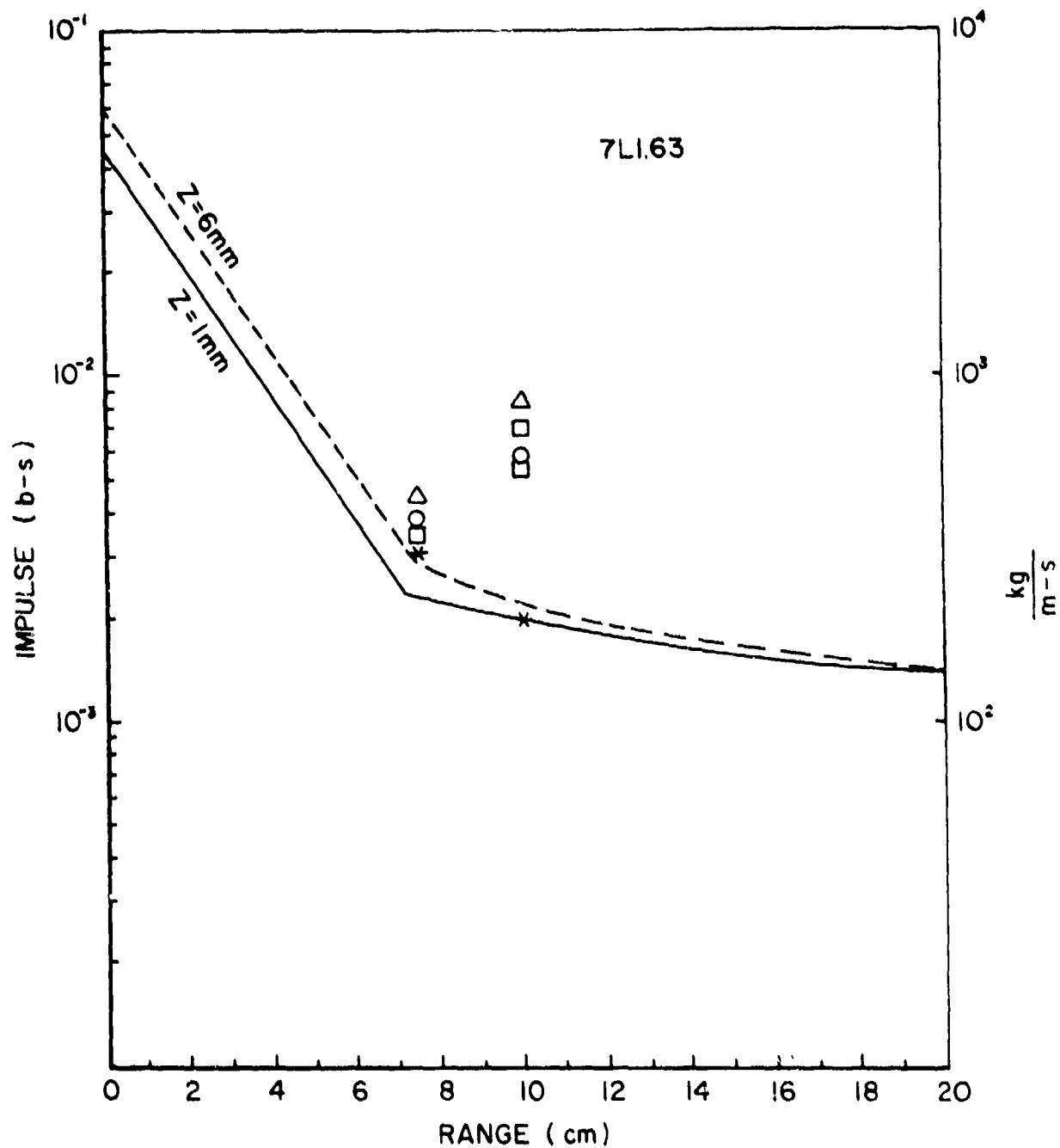


Figure 4.7. Model impulse (equation 12) compared with experimental data, normalized to 7L1.63 configuration (Symbols are explained in Table XII).

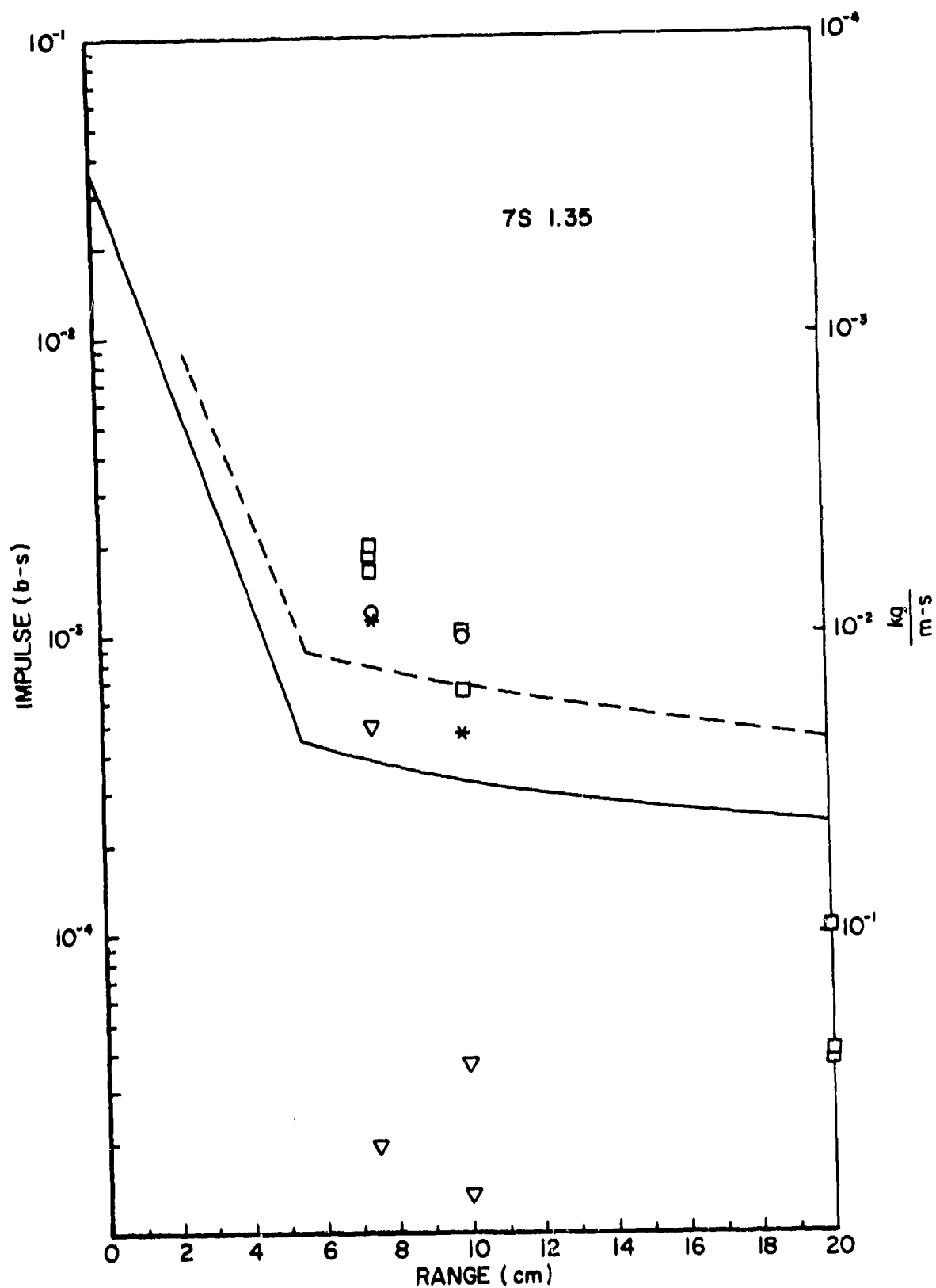


Figure 4.8. Model impulse (equation 12) compared with experimental data, normalized to 7S1.35 configuration.

by impact stresses in the aluminum; their duration was amplified by the formation of a broad shock front in the water by shear and spall failure of a "plug" of aluminum that was often wider than the projectile. The first experimental peak in Figure 4.6 is believed to be associated with this process.

The calculation for a foam-backed panel indicated that near the panel peak pressures were reduced by a factor two, near the impact site and were similar to a non-foam panel at greater ranges. This is consistent with the data in Table X. Details of the calculations are presented in Part II.

#### 4.2 DISPLACEMENT

The NONSAP structural response code was driven with the loading function described in Equations 3 through 9.

The numerical results were compared with the experiments. This comparison was an excellent way to check the code, because displacement was the primary variable in the code. The numerical results, presented in Part II of this report, were not fully satisfactory.

The case examined was 7L1.63, which corresponds to Shot FD8. This was a valuable test case, since it was just below the failure threshold for 6.35-mm-thick 7075-T6 panels.

Figure 4.9 shows a comparison of the experimental and numerical results. The actual peak displacement was more than twice the predicted value. In addition, the calculated panel shapes were qualitatively wrong; the analysis predicted that an inflection would be present at 1.2 ms and a rebound at 1.6 ms. Neither was observed.

Two sources for the discrepancy were diagnosed. The first concerned neglect of the late-time effect of the water behind the panel. The typical duration of the loading function was only 75  $\mu$ s. At later times, there was no forcing function input to the NONSAP code. In reality, a late-time pressure was exerted on the panel equal to  $1/2 \rho (\dot{w} - u)^2$ , where  $\dot{w}$  is the panel velocity and  $u$  is the z-component of the water velocity.

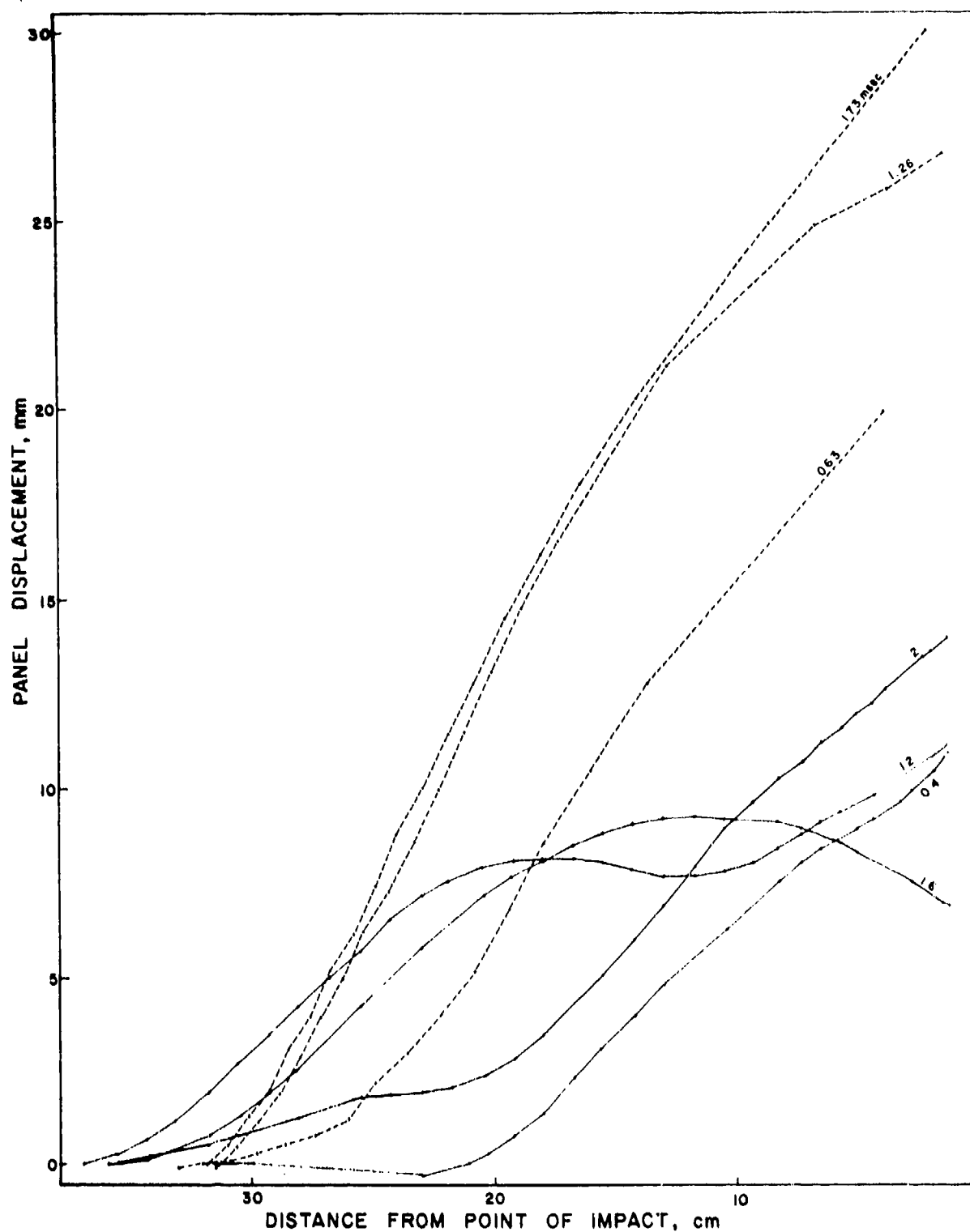


Figure 4.9. Comparison of measured (dashed lines) and predicted (solid lines) displacement for FD8 (configuration 7L). (The measured profiles are referred to the original surface, translated outward to maintain zero displacement at the bolt centerline).



In order to assess the importance of this effect, CRT reran the calculation with a late-time constant pressure equal to 5 bars; this was the approximate average value of  $1/2 \rho u^2$  when the CRALE calculation was terminated. The resulting displacements were significantly closer to the measured ones.

Another possible error was the decay of the loading function with range for  $r > 10$  cm. The original model contained an exponential decay. Limited results from the individual AFTON calculations indicated, however, that integrated impulse in the loading function was nearly independent of the range beyond  $r = 10$  cm. To see if this effect contributed to the errors in calculated displacement, another NONSAP calculation was run in which the total impulse was kept constant for  $r > 10$  cm.

Each of these two corrections accounted for about half the discrepancy, and the rebound and inflection points were eliminated. We, therefore, believe that the presence of these two effects is an accurate diagnosis of the problem. However, there are still some difficulties with the analytic approach to displacement, principally in the revised calculations prediction that the peak displacement would occur at 1.2 ms, which was much too soon.

Another possible cause of the discrepancy was the difference between the shapes of the actual and model panels. The panel in the numerical work was round with a 37.5 cm radius, but the experimental panel was square with 60 cm on a side. Two lines of reasoning indicate that panel shape had only a minor effect on displacement and that the effect was to increase the numerical value. The round-trip travel time for the deflection wave from the impact to the edge was 2 ms. This is less than or comparable to the time of peak deflection, so from this argument it seems that panel shape should have had little effect on calculated displacement. A similar conclusion can be reached by considering the panel displacement resulting from a constant load. Handbook formulas are available for deflection of

circular and square membranes subjected to uniform loads with fixed boundaries.<sup>3</sup> For a circle, the peak deflection is

$$Y_C = 0.171 \frac{Pr^4}{Et^3} \quad (19)$$

whereas for a square

$$Y_S = 0.0138 \frac{Pa^4}{Et^3} \quad (20)$$

In these formulas, E is Young's Modulus, P is the load, r is the radius, a is the side length, t is thickness, and Poisson's ratio is 0.3. For a circumscribed circle ( $2r = a$ ),  $Y_C/Y_S = 0.773$ . For the case at hand ( $r = 37.5$  cm,  $a = 63.5$  cm),  $Y_C/Y_S = 1.50$ . Thus, for late times, a circular panel should experience a larger deflection than a square one. The numerical results were the opposite.

We believe that the most probable major cause for the displacement discrepancies was the way that the late-time loading of the panel was handled. A more accurate treatment could be based on piston theory<sup>1</sup> and would express the driving pressure as a function of the panel velocity. The piston theory approach becomes increasingly accurate as radial pressure gradients decline; this is the case at late times. Piston theory could not be used with NONSAP without extensive recoding. Boundary effects and uncertainties in the loading function beyond 10 cm from the impact also probably contributed to the discrepancy.

#### 4.3 PREDICTION OF FAILURE VELOCITY

Originally it was planned to predict the failure velocity a priori for all combinations of panels and projectiles of interest. For this purpose the hoop stress in the panels were

examined by the NONSAP code. However, it became apparent that the dependence of peak hoop stress on impact velocity was relatively weak, and material failure criteria used in the code were probably not precise enough to identify failure. The approach taken was to "calibrate" the failure criterion by providing CRT with the results of one test case. Accordingly, the failure velocity for configuration 7L was provided.

A failure criterion was established based on the integral  $\int (\sigma_\theta - Y) dr$ , where  $Y$  is the yield strength. The critical value was taken as that computed for the 7L configuration at the failure threshold. Part II of this report described in detail how, using this technique, a number of failure predictions were made. The failure predictions are summarized in Table XIV, where they are also compared with the experimental data.

Overall, the success of failure predictions was judged poor. Although many failure points were not checked by experiments because the values were extremely high or low, the data do allow a few critical comparisons.

TABLE XIV. SUMMARY OF FAILURE PREDICTIONS

<u>Designation</u>	<u>V<sub>F</sub> Predicted</u>	<u>V<sub>F</sub> Measured</u>
<u>7L</u>	-----	1.65 ± .02
<u>7S</u>	1.92-1.97	>1.95
7L	1.33-1.41	<0.99
7S	1.48-1.58	0.99 ± .07
<u>2L</u>	1.60-1.64	>1.62
2L	1.45-1.46	<1.32
2S	1.60-1.64	1.77 ± .17*

\*Determined for a spherical projectile.

For those cases in which a critical comparison is possible, viz 7S, 7L, and 2L, the predictions were significantly in error. We believe that a number of factors contributed to the inability of the analysis to predict failure:

- Peak stress in the panel was not resolved precisely enough in the impact region.
- Adequate triaxial tensile failure data for the panel materials were not used.
- Initial pressures were higher than predicted because of higher effective drag forces on the cubical projectiles and debris.
- Stress concentrations occurred at the corners of the perforation made by the cubical fragments. Especially in thin panels, this led to significantly lower failure thresholds than might otherwise have existed.

## SECTION V

### SUMMARY OF DATA FOR PANEL DESIGN

This section collects the results of experiments and calculations and formulates design guidelines. The design guides have regions of uncertainty, but we believe that they represent the best inferences that can be made from the presently available experimental and numerical data.

#### 5.1 FAILURE CRITERIA

We analyzed failure of plain aluminum panels, since that was the easiest case, and we used the insight gained to treat graphite epoxy panels. Finally, we examined the data for foam-backed panels and stiffened panels.

##### 5.1.1 Failure of Plain Panels

Failure criteria must be fixed by experimental data, since the calculations were not accurate enough to stand alone. For given panel, liquid, and projectile materials, the failure criteria must take the form

$$f(V_F, D, W) = \text{constant} \quad (21)$$

The constant in Equation 21 was termed the failure constant and is denoted by  $F$ . The principal problem was to determine the form of the function  $F$  so that  $F$  could be evaluated and failure criteria could be extrapolated from the data. By assuming that the inaccuracies in the numerical failure predictions were caused mainly by inaccuracy in the failure constant, we used the numerical results to determine

$\partial V_F / \partial D|_W$  and  $\partial V_F / \partial W|_D$ . This assumption could not be rigorously defended, but it could be partly justified by the following arguments: (1) The calculations reproduced the pressure in the water; thus, they probably correctly treated

the important physical phenomena that led to panel strain and failure. (2) The most likely cause of error was inadequate failure criteria for aluminum. In most models this would only affect the value of the constant in Equation 21 (see discussion below).

The data by themselves were complete enough to determine empirically the failure constant or the functional form of  $F$ . Therefore, we considered a small number of reasonable physical models for penetration that resulted in particular forms for  $F$ . We selected forms that did not contain any additional empirical parameters. The relative validity of these forms could be distinguished from the data by the degree to which  $F$  was indeed constant.

To derive phenomenological models, we assumed that the panels failed because of forces exerted at early times near the impact site. If a displacement  $\delta$  occurred at the edge of the perforation and the displacement only extended for a distance  $r$  beyond the perforation, then the hoop strain would be approximately  $2\delta^2/r^2$ . To reveal the gross dependence of hoop strain,  $\epsilon_\theta$ , on impact variables, we used  $\delta = \frac{1}{2}at^2$ . The acceleration,  $a$ , was found from the force/unit mass on the panel,  $\bar{P}t/\rho_t W$ . Here  $\rho_t$  is panel density, and  $\bar{P}$  is the average pressure exerted on the panel during time  $t$ . The width of the affected region of the panel was  $r = Ct$ , where  $C$  is the shock speed in water. Thus,

$$\epsilon_\theta = \frac{P_t^2 t^2}{2\rho_t^2 C W^2} \quad (22)$$

The impulse delivered to the water during the penetration ( $0 \leq t \leq W/V$ ) was approximately

$$\int (P_H \frac{D}{W} + \rho V^2) dt = \frac{P_H D^2}{C_p W} + \rho W V \quad (23)$$

where  $P_H$  is the pressure resulting from a one-dimensional impact,  $\rho$  is the liquid density, and  $C_p$  is the speed of sound in the projectile. The shock was assumed to attenuate approximately as  $1/r$  and to be of duration  $D/C_p$ ; the duration of the penetration was  $W/V$ .  $P_H$  could be related to  $V$  by an impedance-match solution<sup>4</sup>, the results of which are shown in Figure 5.1. Over the range of interest, the pressure in the water was almost a linear function of impact velocity given by

$$P_H = A_H V \quad (24)$$

$$A_H = 68 \frac{\text{kbar}}{\text{km/s}} \quad (25)$$

When the right-hand side of Equation 23 was evaluated for thin panels, the first term was found to be about three times the second; for thick panels, the first was about 30 times the second. Thus, for early times, the shock pressure dominates the drag pressure. Substituting Equation 23 for  $P_t$  in Equation 22 and ignoring the drag term gave

$$\epsilon_\theta = \frac{A^2 V_D^2}{2 \rho^2 C_p^2 W^2} \quad (26)$$

If failure was associated with a maximum value of  $\epsilon_\theta$ , then the form of Equation 26 would become

$$\text{MODEL I} \quad F_1 = \frac{VD}{W} = \text{constant} \quad (27)$$

As expressed in the form of Equation 27 and throughout this report, large values of the failure constant are associated with materials that are relatively resistant to failure.

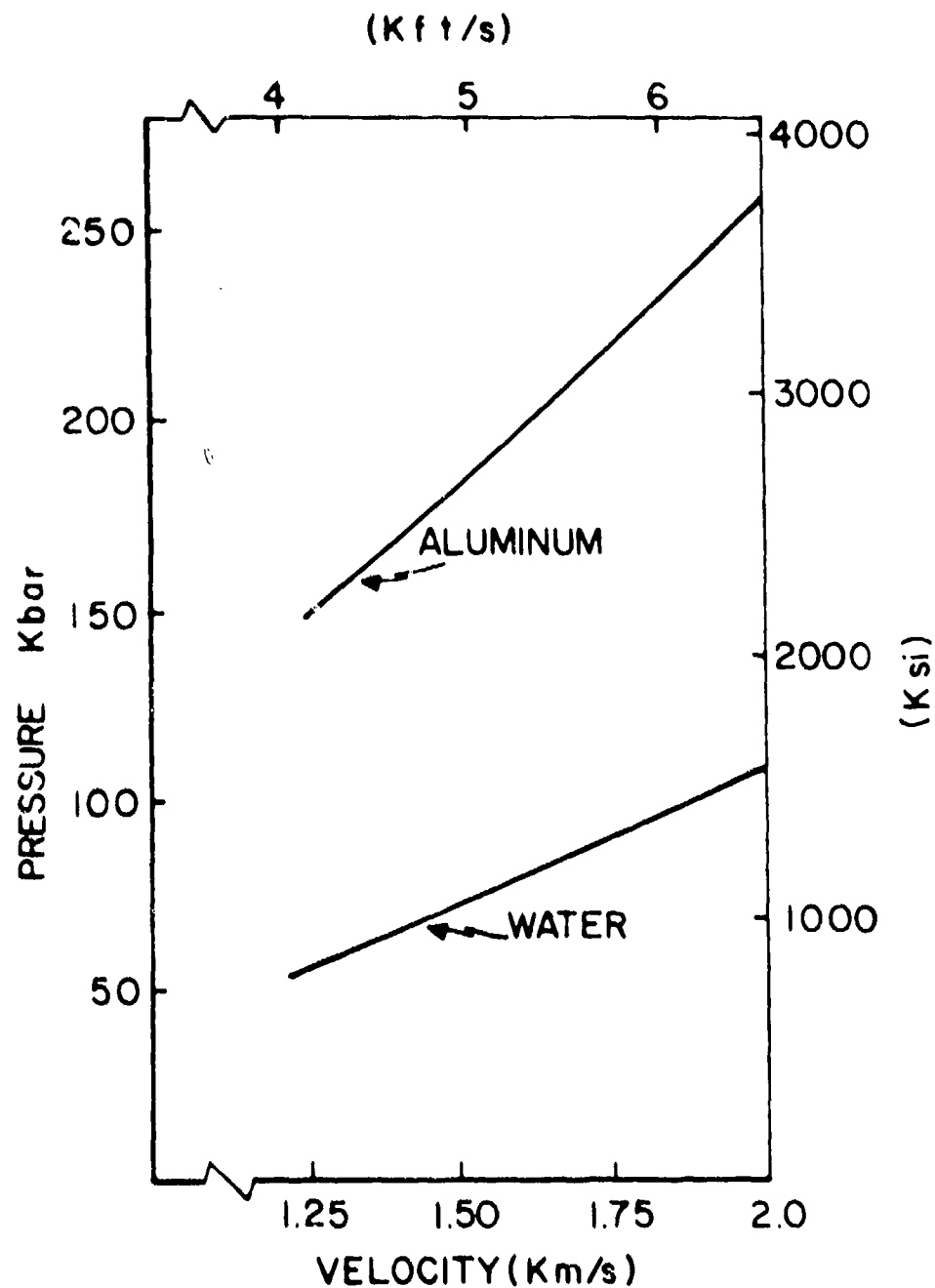


Figure 5.1. Theoretical one-dimensional impact pressure for impact of a steel projectile on a water-backed aluminum plate.



A simpler model resulted from assuming that failure is proportional to the impact energy,  $\frac{1}{2} \rho_p D^3 V^2$ , divided by the panel volume,  $L_p^2 W$  (where  $L_p$  is the panel edge dimension). This model led to the failure criterion

$$\text{MODEL II} \quad F_2 = \frac{V^2 D^3}{W} = \text{constant} \quad (28)$$

Note the higher powers of  $V$  and  $D$  than in Equation (27).

Model III was based on prompt shock energy. The energy per unit volume in a shock wave is  $\int P_H dt$ . At any given position in our tank, this value was proportional to  $P_H u_H t_s$ , where  $u_H$  is the particle velocity associated with the initial shock pressure and  $t_s$  is the shock duration. If we substitute the Rankine-Hugoniot relationship,<sup>5</sup>

$$u_H = \frac{P_H}{\rho_0 C_0} \quad (29)$$

and  $t = D/C_p$  gave

$$P_H u_H t = \frac{P_H^2 D}{\rho_0 C_0 C_p} \quad (30)$$

Failure would be caused by this energy being imparted to a wall section of thickness  $W$ ; this observation led to the failure criterion

$$\text{MODEL III} \quad F_3 = \frac{V^2 D}{W} = \text{constant} \quad (31)$$

We could have formulated other models involving more adjustable constants and corresponding to a more sophisticated view of the failure process. However, the data base

was insufficient to provide a value for more than one empirical failure parameter. The analysis for selecting among models I, II, and III is presented in Table XV.

In order to evaluate the models, it was necessary to construct the largest possible data set. Although some numerical failure predictions were inaccurate, we assumed that the errors were systematic (such as would result from an erroneous failure criterion for aluminum). In that case, numerical results could still be compared with each other in order to determine the dependence of failure on parameters which were treated correctly. We made the following sets of comparisons of the entries in Table XV: 1-2, 1-4, 3-4, 6-7, 8-11, 10-12. The entry on line 10 was not used because the failure velocity was anomalous with respect to the other calculations. Of the six comparisons, average (absolute) values of percentage differences in the failure constant for models were as follows:

Model I: 23%

Model II: 37%

Model III: 10%

Model III was clearly the best. Moreover, since it contained  $V$  to the second power, the expected error in  $V_F$  (all else being equal), is only about 5 percent. Model I was almost as good as Model III, if one ignored the comparison between the experiments in 1-3. However, this is a crucial comparison because it involves a large contrast in  $V$ .

Design curves for prevention of failure in plain panels are given in Figures 5.2-5.6. Model III was used in these figures, and the failure constant was evaluated separately for each curve for which there was an experimental data point. For curves that did not go through data points, average values of the failure constant were used. Table XVI summarizes the recommended values for the failure constant. Note that the

TABLE XV  
EVALUATION OF FAILURE CRITERIA  
(uni' s used are mm and  $\mu$ s)

Line	Configuration			$V_F$	Source	Values of Failure Constant		
	ID	D	W			Model I ( $VD/W$ )	Model II ( $V^2D^3/W$ )	Model III ( $V^2D/W$ )
1	7L	11.41	6.35	1.65	experiments	2.96	637	4.89
2	7S	8.97	6.35	1.95	numerical	2.75	432	5.37
3	7L	11.41	1.6	1.33	numerical*	9.48	1642	12.6
4	7S	8.97	1.6	0.99	experiment	5.55	442	5.5
5	7S	8.97	1.6	1.53	numerical*	8.58	1055	13.1
6	GS	11.08	4.8	1.47	experiment	3.39	612	4.99
7	GS	11.08	3.18	1.09	experiment	3.79	508	4.14
8	2L	14.25	6.35	1.65	numerical*	3.7	1241	6.11
9	2S	11.08	1.6	2.08	experiment	14.4	3678	30.0
10	2S	11.08	1.6	1.62	numerical*	11.22	2231	18.2
11	2S	11.08	6.35	1.96	numerical*	3.42	840	6.70
12	2L	14.25	1.6	1.46	numerical*	13.0	3855	19.0

\*May be in error by 50 percent.

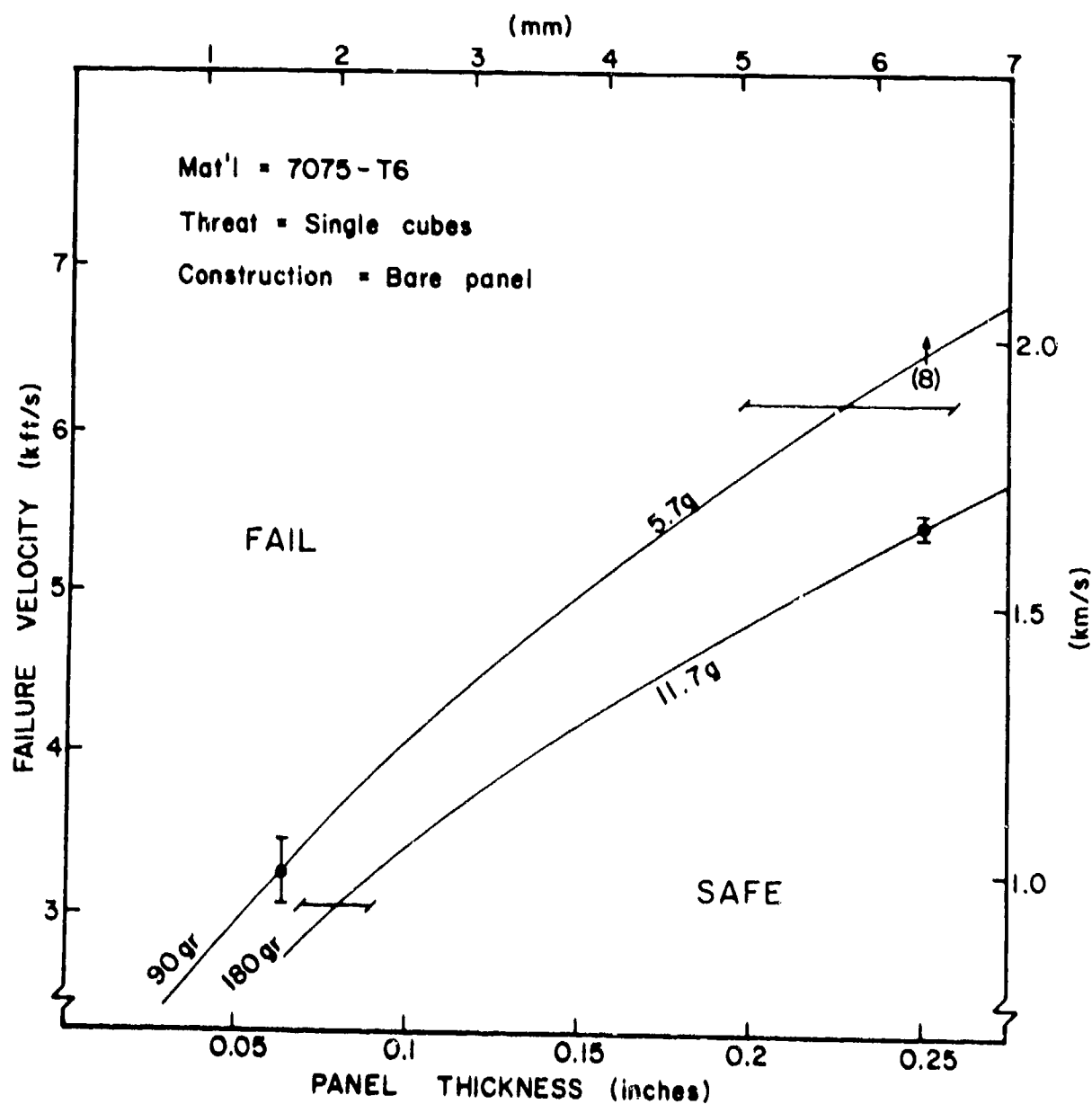


Figure 5.2. Design summary for bare 7075-T6 aluminum panels struck by single cubical fragments. Failure velocity versus panel thickness for various mass fragments, based on  $v^2 D/W = 5.19 \text{ km}^2/\text{s}^2$ .

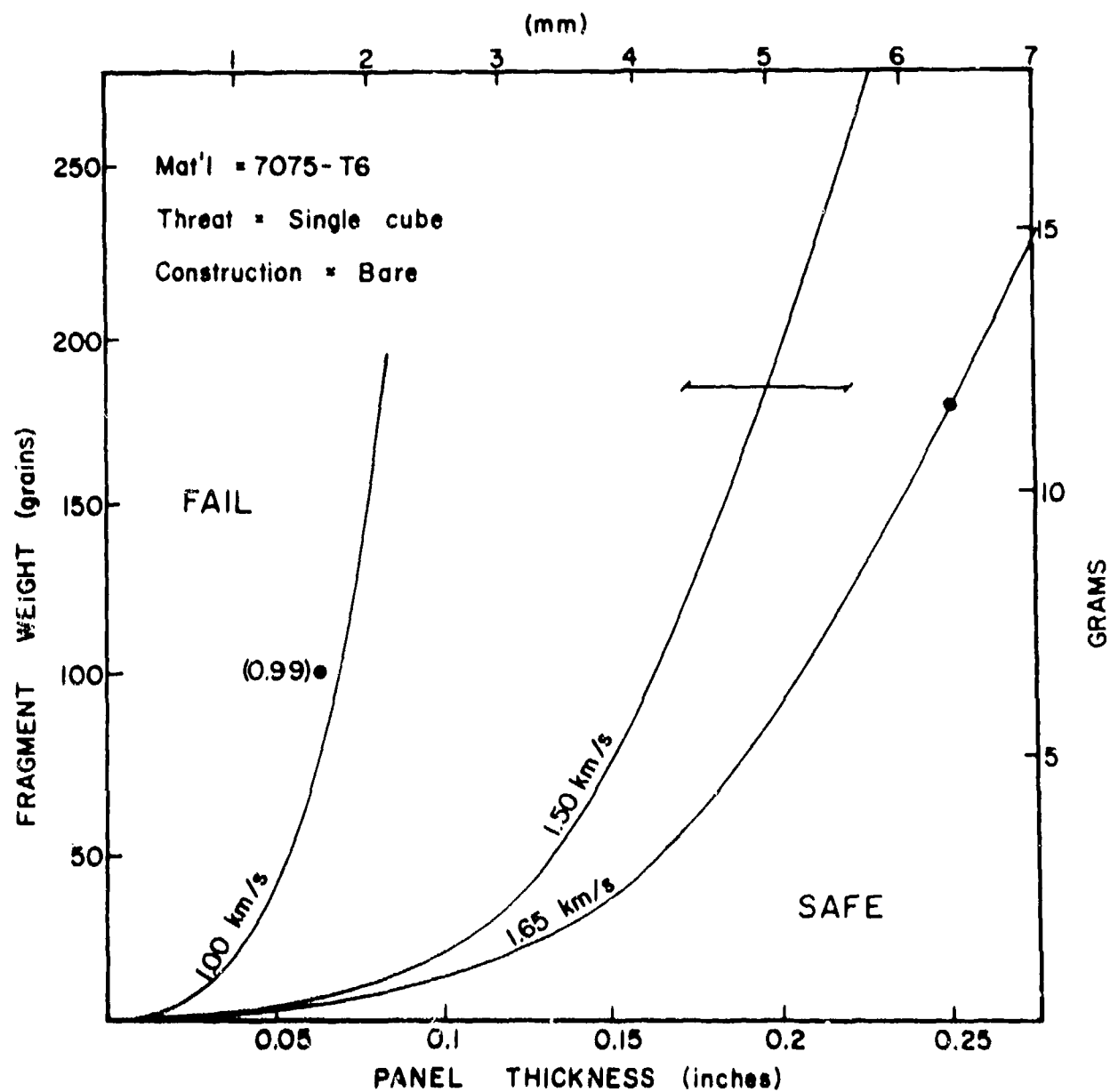


Figure 5.3. Design summary for bare 7075-T6 aluminum panels struck by single cubical fragments. Fragment mass versus panel thickness for various velocities, based on  $v^2 D/W = 5.19 \text{ km}^2/\text{s}^2$ .

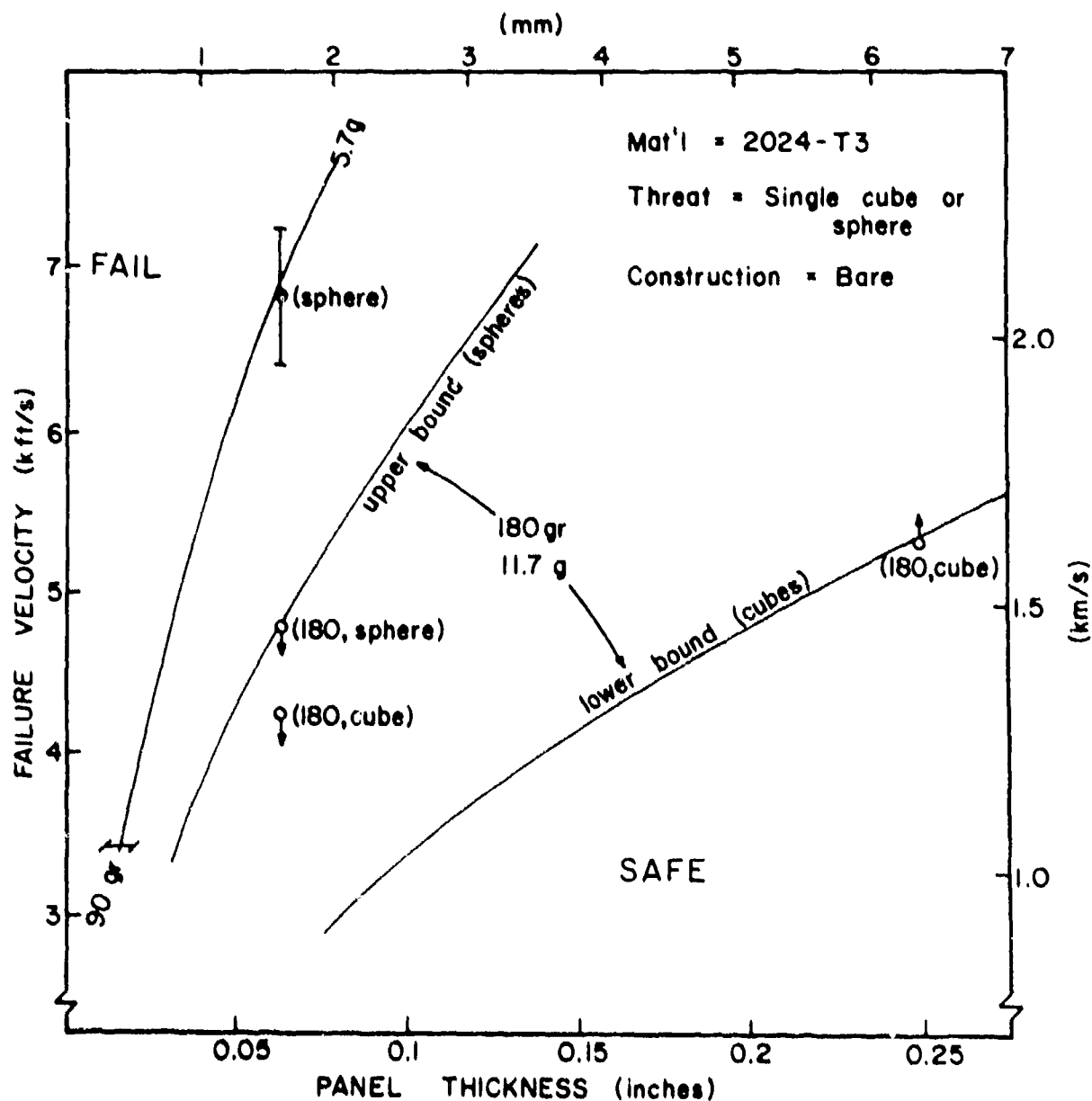


Figure 5.4. Design summary for bare 2024-T3 aluminum panels struck by single cubical fragments. Failure velocity versus panel thickness for various mass fragments, based on  $v^2D/W = 30 \text{ km}^2/\text{s}^2$  for 90 g cubes,  $19 \text{ km}^2/\text{s}^2$  for 180 g spheres, and  $12 \text{ km}^2/\text{s}^2$  for 180 g cubes.

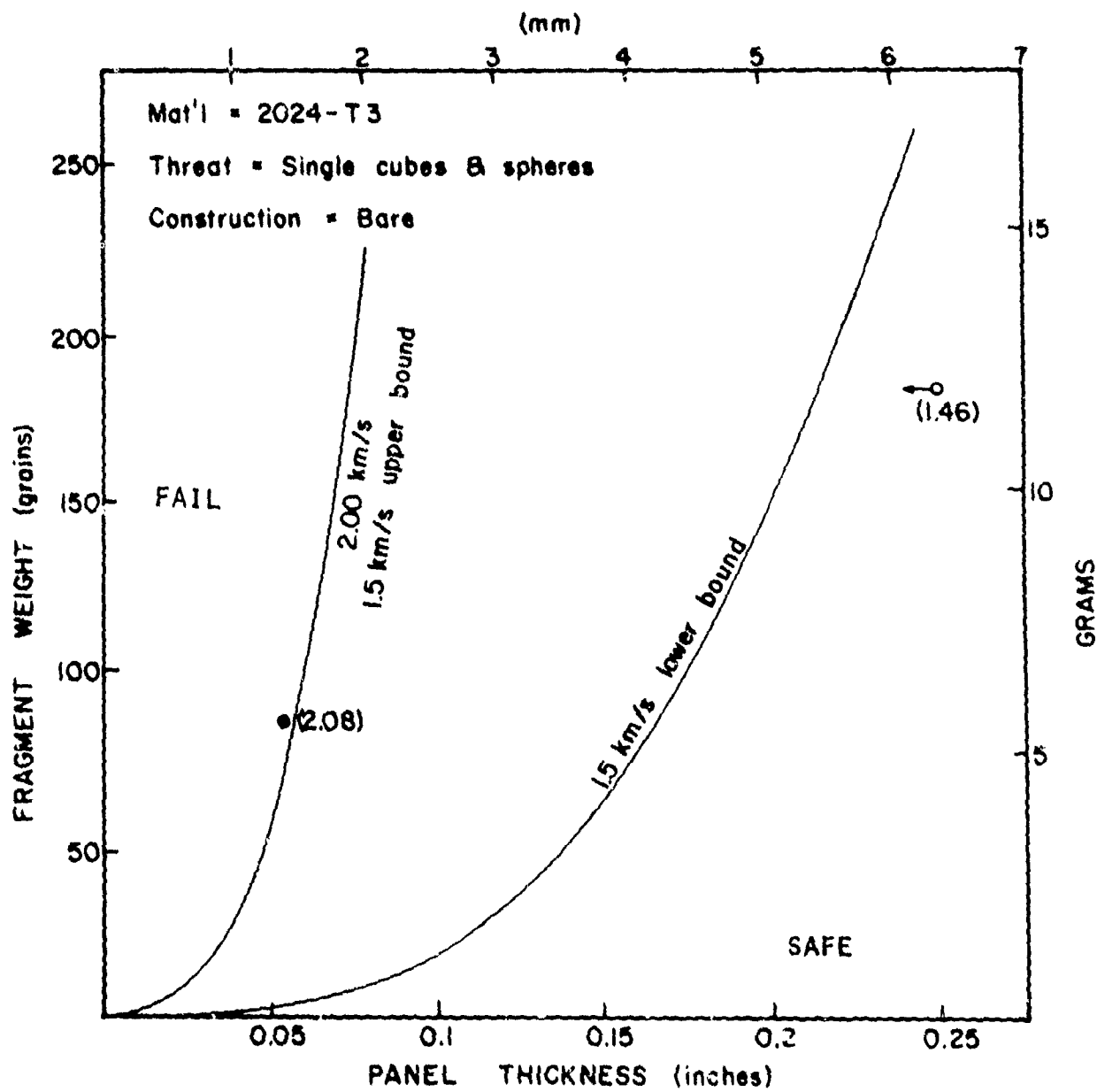


Figure 5.5. Design summary for bare 2024-T3 aluminum panels struck by single cubical fragments. Fragment mass versus panel thickness for various velocities, based on  $v^2D/W = 30 \text{ km}^2/\text{s}^2$  for 90 g projectiles and  $19.0 \text{ km}^2/\text{s}^2$  for 180 g projectiles.

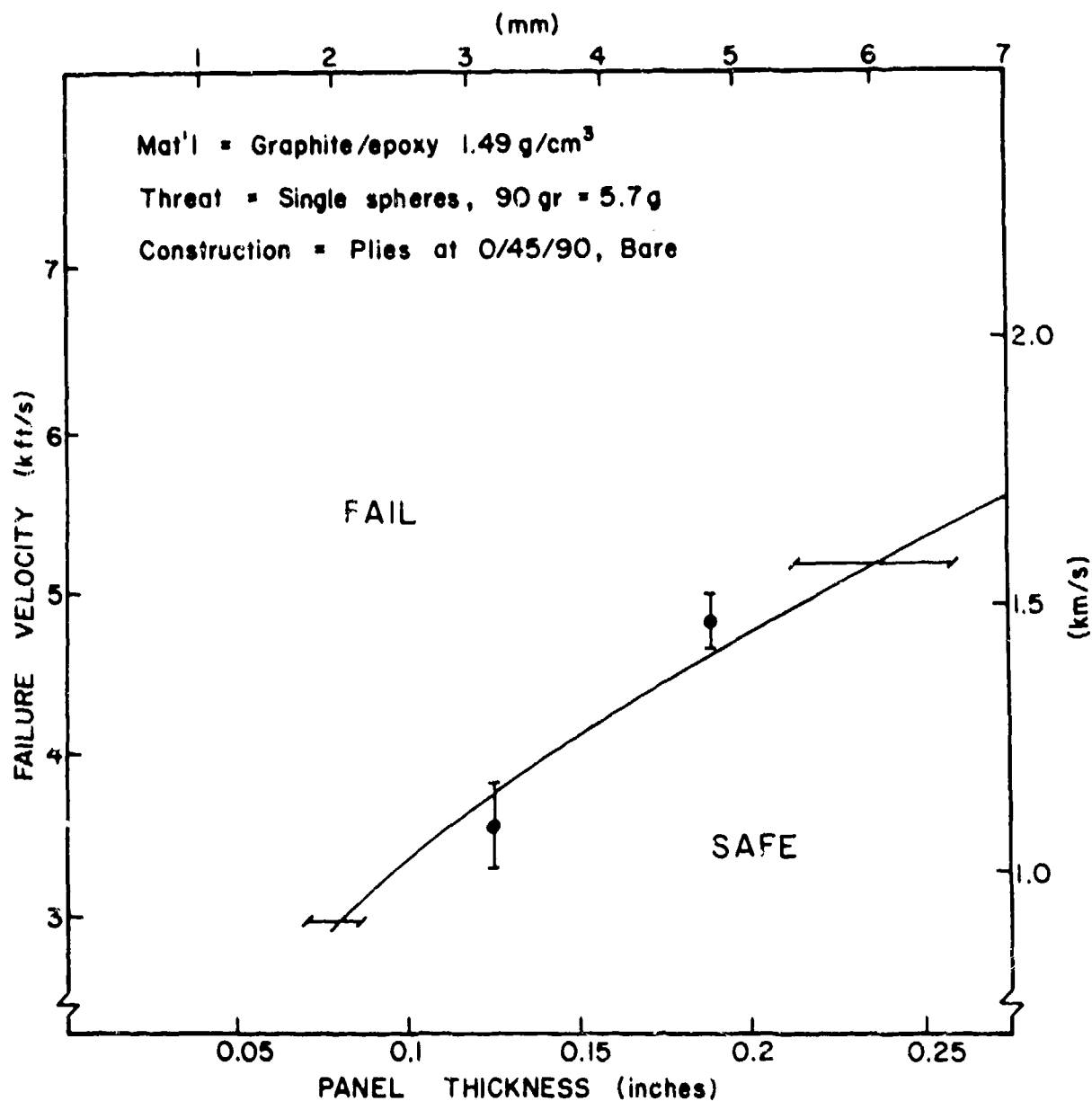


Figure 5.6. Design summary for graphite epoxy panels struck by single cubical fragments. Failure velocity versus panel thickness for 5.7 g fragments, based on  $v^2 D/W = 4.53 \text{ km}^2/\text{s}^2$ .



parameter D in this table represents cube edge or sphere diameter.\* Experimental data points for failure velocity are shown as filled circles with error brackets; open circles with an arrow are used where the failure threshold was only bounded on one side. Numerical results of relatively high confidence are shown as open circles with parenthesis. Uncertainty ranges are indicated on the curves when appropriate. Uncertainty along any curve grades from the uncertainty in data points to the uncertainty range indicated for the curve.

TABLE XVI  
RECOMMENDED VALUES OF FAILURE CONSTANT

$$\frac{V^2 D}{W} = F_2$$

Material	$F_2 \text{ (km/s)}^2$
7075-T6 aluminum	$5.19 \pm 0.3$
2024-T3 aluminum	$30.0 \pm 5.7$ (for 90-grain fragments) $19.0$ (for 180-grain fragments)
graphite epoxy (1.49 g/cm) <sup>3</sup>	$4.52 \pm 0.47$

Figures 5.4 and 5.5 for 2024-T3 aluminum presented some special difficulties. First, the available data were for both cubical and spherical projectiles. Second, the value of the failure constant from line 9 of Table XV,  $30 \text{ mm}^2/\mu\text{s}$ , leads to a failure velocity prediction for 2L (spherical projectile of  $1.83 \pm 0.17 \text{ km/s}$ , (shot FTA9, Reference 2)). The source of this discrepancy may be that certain assumptions embodied in Model 3 are not valid when configurations 2S and 2L are compared; for example, the failure may have been slower

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\*The rationale for this is that in models I and III, D represents the distance waves must travel in the projectile to encounter free surfaces and generate relief waves.

than predicted because the 2024-T3 alloy was relatively more ductile than the 7075-T6 alloy. Longer time scales imply that drag forces were relatively more important, hence, that failure was more dependent on a higher power of  $D$ , as in Model 2. We also noted that the failure constants for both 2S numerical and 2s experimental (lines 9 and 10 of table XV) were significantly higher than those for  $\underline{2L}$ , 2L, and  $\underline{2S}$ . To model failure for the 2024-T3 aluminum struck by the large projectiles, we used the failure constant from line 12 as an upper bound ( $19.0 \text{ mm}^2/\mu\text{s}^2$ ). We believe this lower bound to be very conservative because it practically coincides with the failure criterion for 7075-T6 panels struck by 180-grain fragments. Cubical fragments are evidently more lethal than spherical fragments; we took this factor into account in Figure 5.4 by using cubical fragment data for the lower bound and spherical fragment data for the upper bound. In Figure 5.5, because of the large differences in failure constants, the upper bound for the 1.5 km/s curve fell above the 2.0 km/s curve; as this is physically unreasonable, the two curves were drawn coincident.

#### 5.1.2 Failure of Graphite Epoxy Panels

Model II fitted the limited data for graphite epoxy panels very well. The results are shown in Figure 5.6. The data set provided a good test for the model because failure thresholds were determined for two different panel thicknesses struck by the same projectile.

#### 5.1.3 Failure of Foam-Backed Panels

Foam was extremely effective for preventing gross panel failure. In fact, in all of the configurations that the University has examined to date, failure has been experimentally produced in only one foam-backed case--7LF. Numerical predictions of failures in foam-backed panels have also been unsuccessful.

The experimental and numerical results presented in Section 3 and Part II of this report showed that failure did not initiate immediately after impact in foam-backed panels. Therefore, the initial shock wave was not the failure mechanism in foam panels. Model III was not appropriate for this situation.

Rather, we examined the total impulse delivered to the panel.

The numerical calculations showed that the total impulse at a given range,  $r$ , is proportional to  $V_0^{-\alpha r/D}$ . When one integrates from the impact site out to large  $r$ ,

$$\frac{\text{total impulse on plate}}{\text{plate thickness}} \sim \frac{VD}{\alpha W} \quad (32)$$

From the numerical work,

$$\alpha = 0.185 + .35 \exp(-.03 \rho_e W_e) \quad (33)$$

where  $W_e$  and  $\rho_e$  are the equivalent wall (panel plus foam) density and thickness. The exponential is mainly determined by the foam properties, not the panel properties, so this term was regarded as constant. Then the term  $\alpha$  could be included in the constant  $F_1$ , and Equation 32 became

$$F_1 = 11.55 \text{ km/s.} \quad (34)$$

This value was used to construct the graphs shown in Figures 5.7 and 5.8. The model predictions are consistent with the data. A "best guess" uncertainty range of  $\pm 15$  percent was assumed for  $F_1$ . Comparison of Figure 5.7 with Figure 5.2 illustrates that the presence of foam more than doubles the failure velocity.

Failure of foam-backed 2024-T3 panels required higher impact velocities than could be experimentally achieved. A reasonable extrapolation of Equation 34 would be to multiply  $F_1$  by 1.35, since the strain-energy to failure of 2024-T3 aluminum is 55 percent higher than that of 7075-T6. However, this process yielded a prediction of  $V_F$  for the 2SF configuration of 1.67 km/s, whereas the observed value is  $V_F > 2.38$  km/s. At this time, we do not feel that it would be useful to predict failure thresholds for foam-backed 2024-T3 alloy. The best we can do is to restate the observed lower limits for failure:

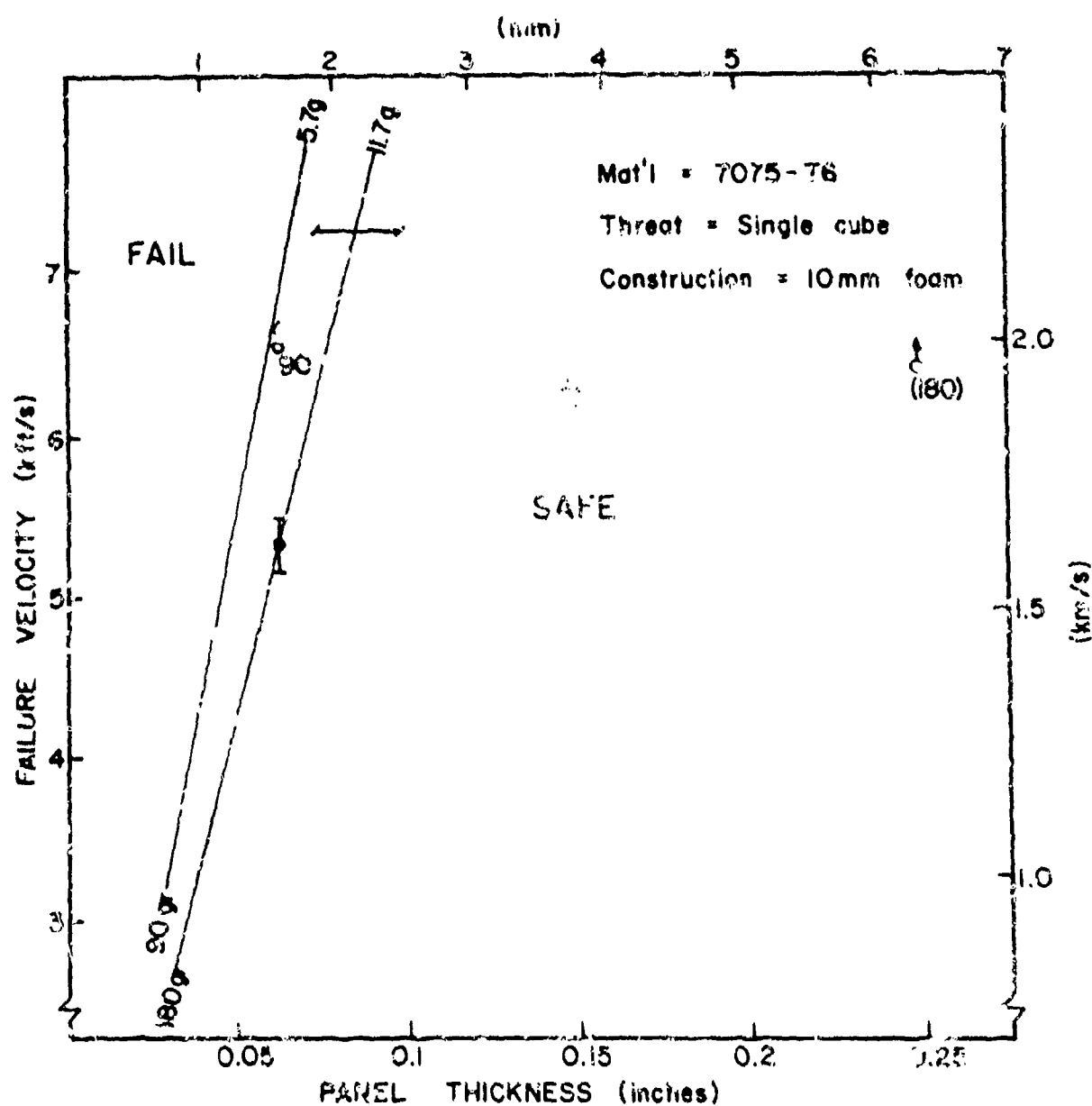


Figure 5.7. Design summary for 7075-T6 foam-backed panels struck by single cubical fragments. Failure velocity versus panel thickness for various mass fragments, based on  $VD/W = 11.55 \text{ km/s}$ .

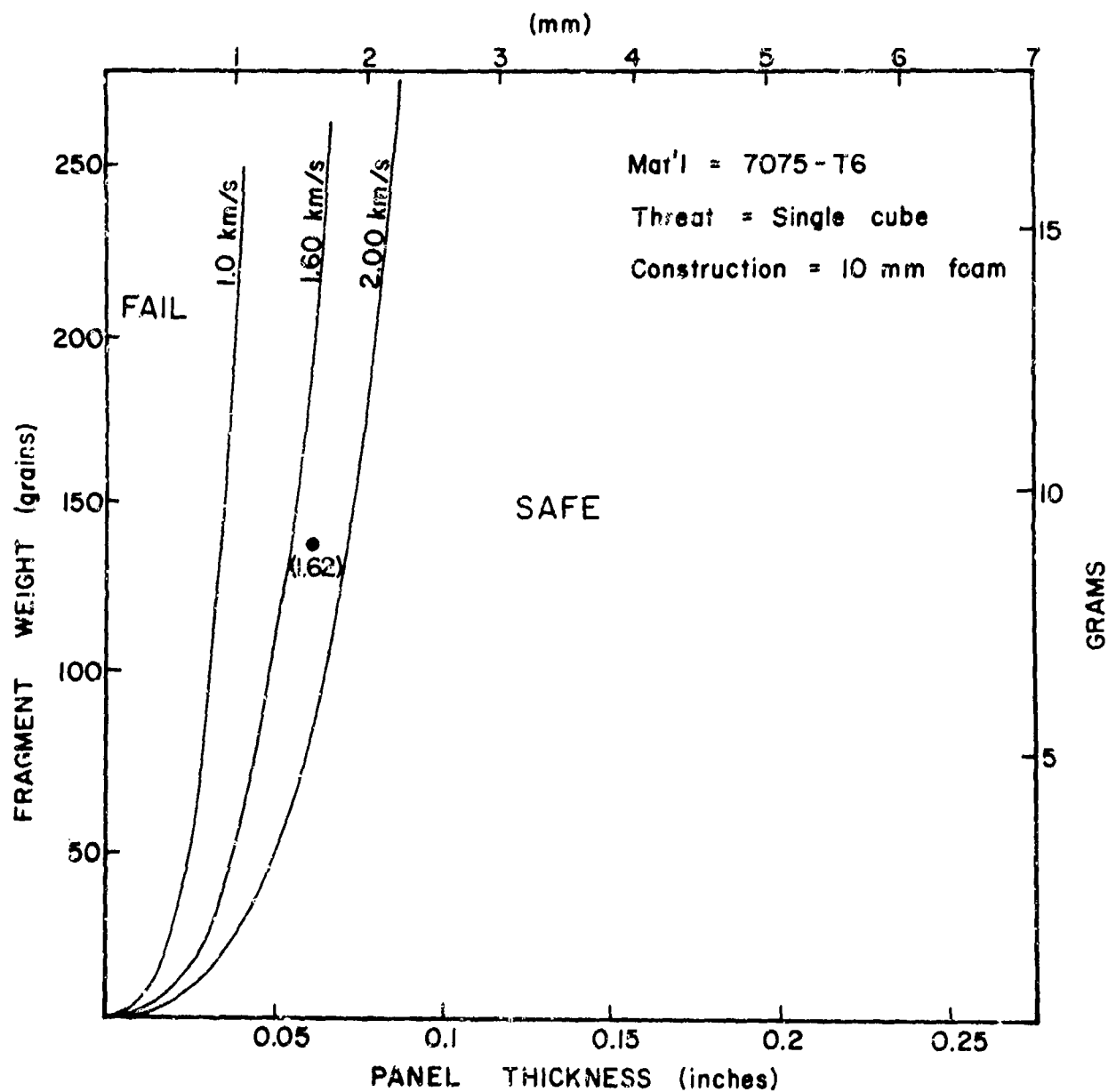


Figure 5.8. Design summary for foam-backed 7075-T6 panels struck by cubical projectiles. Fragment mass versus panel thickness for various velocities, based on  $VD/W = 11.55$  km/s.

$$2LF \quad V_F > 1.69 \text{ km/s}$$

$$2SF \quad V_F > 2.38 \text{ km/s}$$

The data in Figures 5.7 and 5.8 will also yield a conservative design for 2024-T3 panels.

#### 5.1.4 Failure of 2024-T3 Panels Stiffened With 6063-T6 Hat Sections

Addition of stiffeners changed the failure modes of panels, as we discussed in Section III. Massive tears no longer occurred; cracks only ran a short distance to stiffener rivet holes, then stopped. Failure was associated with stiffener crushing and rivet failures. A distinct failure threshold existed, below which permanent stiffener deformation was negligible and no rivets failed, and above which serious plastic deformation of stiffeners occurred and many rivets failed.

The stiffeners used in the study were made from 6063-T6 aluminum, which has a handbook yield strength of 1.4 kbar 21 (ksi). The results described below are probably not applicable to stiffeners made from stronger alloys (2024-T3 has a yield strength of 3.2 kbar). The construction of the test panels is shown in Figures 2.3 and 2.4.

Only one value of panel thickness was included in the test matrix, so the  $W$  dependence of  $f$  in Equation 21 could not be determined. However, since the observed failure was stiffener crushing, the panel thickness may or may not be a critical parameter.

Two failure points were determined. Of Models I through III, Model III fits the data more than twice as well as the others. This only showed adequate scaling for velocity and projectile size. The effects of variations in panel thickness and stiffener geometry could not be determined from the data. For this reason, the design curve in Figure 5.9

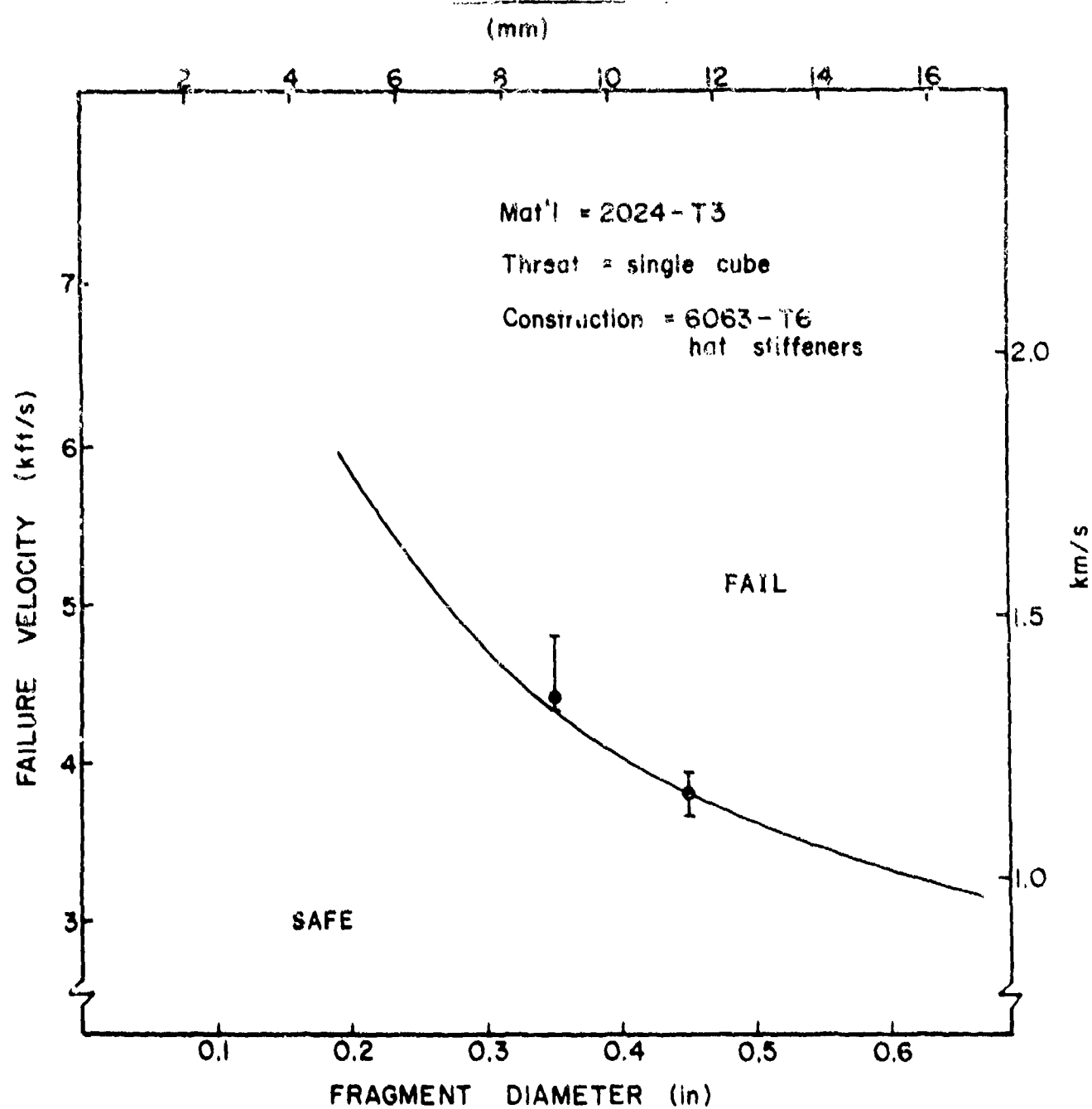


Figure 5.9. Design data for 2024-T3 aluminum panels protected with "hat" stiffeners, based on  $V^2 D/W = 9.83 \text{ km}^2/\text{s}^2$ .

is plotted as a fragment size versus fragment velocity. The average value of  $F_3$  is  $9.83 \text{ km}^2/\text{s}^2$ .

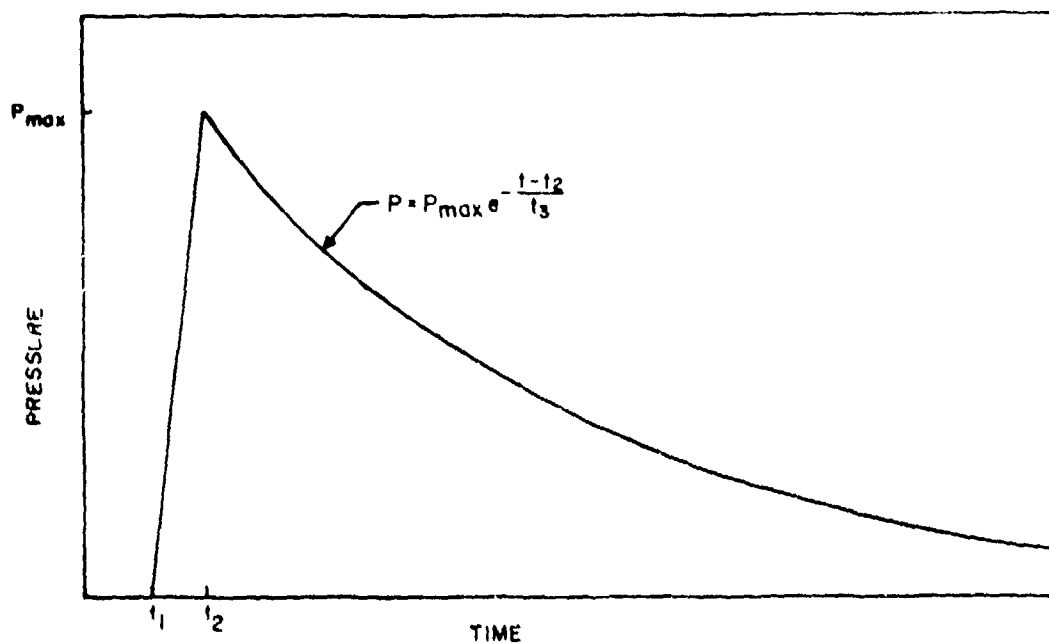
The failure point for panels with foam between the stiffeners was not bracketed. From examination of the foam-backed panels that were shot, it appeared that foam between the stiffeners is not useful for protecting stiffeners from hydrodynamic ram damage. Increased protection could, however, be achieved by applying foam to the stiffeners themselves.

## 5.2 PANEL LOADING FUNCTIONS

The improved loading function developed by CRT is recommended for applications in which structural effects of hydrodynamic ram must be determined. The dependencies of impulse on projectile diameter, projectile velocity, and radial coordinate have been derived from a basically sound numerical analysis; and they fit the data as well as any reasonable model. Some discrepancies have been observed between predicted impulses and experimental data; however, discrepancies have not been well enough determined to substantiate revisions of the model.

When we applied the model to a structural response code, we found it necessary to add a correction to the loading function to account for the resistance to inward panel motion caused by the water behind the panel. The size of the correction and its dependence on impact parameters is yet to be determined. A late-time pressure of 5 bars was necessary to achieve reasonable correlation with observed displacements in a calculation of a 11.67-g projectile at 1.63 km/s. A physically reasonable assumption is that the velocity of the water is proportional to initial projectile impulse. This leads to scaling of the late time correction pressure for that case a factor  $(M_p V/19)$ . Here  $M_p$  is in grams, and  $V$  is in km/s. The resultant model is shown in Figure 5.10.





$$P_m = 3.68 \delta V (R'/R_p)^{-\alpha}$$

$$\alpha = 1.15 + 0.6 \exp(-0.72 \rho_t W)$$

$$I = 0.03 \delta V \exp(-\beta R'/R_p)$$

$$\beta = 0.185 + 0.35 \exp(-0.63 \rho_t W)$$

$$t_2 = W/V + 6.25 (R - R_p)$$

$$t_1 = t_2 - (2W/0.63 + 1)$$

$$t_3 = \min \frac{I/P_m - 0.5 (t_2 - t_1)}{10}$$

$$\delta = R'/r$$

$$R' = \begin{cases} r & r \leq 10 \\ 10 & r > 10 \end{cases}$$

Figure 5.10. Loading function model (symbols are defined in the Symbol Table at the beginning of this report).

### 5.3 DISPLACEMENT

Occasionally, the ease with which a solution is found is commensurate with the solution's unimportance. Such is the case of displacement, which for unfailed panels is probably a relatively minor design parameter. Our data showed that all unstiffened aluminum panels of 1.6-mm to 6.35-mm thickness and of 10 cm x 10 cm linear dimension were displaced about 30 mm. Graphite epoxy panels of similar size were displaced about 25 mm.

## SECTION VI

### SUMMARY AND CONCLUSIONS

For complex problems such as hydrodynamic ram, physical understanding is a great aid in the development of efficient optimum design techniques. Parts I and II of this report have continued the work begun in Reference 2, and taken together they present a generally adequate physical model for hydrodynamic ram damage induced by high-velocity fragments striking entrance panels. The experimental and numerical results from these programs have been used to compile design guidelines.

#### 6.1 EXPERIMENTAL RESULTS

Failure data, displacement data, and pressure data have been obtained from laboratory experiments and finite difference calculations. Panels were made from 7075-T6 and 2024-T3 aluminum and from graphite epoxy; panel thicknesses were 1.6 to 6.35 mm. Protection included 10-mm-ballistic foam and stiffeners. Projectiles were 5.6-g and 11.7-g spheres and cubes.

Failures were always catastrophic, and failure thresholds were always abrupt. When cracks formed, they ran across the panels, except when stiffeners were present. In thin panels, cracks initiated at the corners of the perforation when cubical projectiles were used. The stiffeners used in this study sometimes failed by crushing.

#### 6.2 PHYSICAL MODEL

Entrance panel damage caused by high-velocity fragments was primarily induced by the shock wave generated by the impact. The very high shock pressure resulted in impulsive loading of the panels. Cracks occurred at the entrance site almost immediately, and these were propagated by the displacement field. The dependence of failure on impact parameters could be approximately represented as

$$\frac{V^2 D}{W} = \text{constant}$$

where V is projectile velocity, D is projectile diameter, and W is panel thickness.

Stiffeners that suppressed displacement also suppressed crack growth. The effect of foam was to decouple the initial impact shock from the fluid and to lengthen the time over which the panel experienced impulsive loading; consequently, the peak stress at the entrance site was greatly reduced. Crack initiation was not as prompt in foam panels as in plain panels.

### 6.3 NUMERICAL RESULTS

The CRALE finite difference program was able to calculate pressure caused by shock and drag forces on the projectile. A simplified loading model based on the details of the CRALE results was developed. The results of driving the NONSAP structural response code with the simplified loading model, however, were disappointing. Neither displacement nor failure thresholds were correctly predicted. The error in displacement may be attributed to neglect of late-time water resistance to panel slow down and rebound.

### 6.4 DESIGN DATA

Design data were generated for all configurations studied. Design data included failure criteria and pressure loading functions. The failure data were based on experimental results, but numerical results were used to aid interpolation and extrapolation. The pressure loading functions were based on an analytic fit to the numerical results. Failure of stiffened panels was defined by stiffener crushing.

### 6.5 NEED FOR ADDITIONAL WORK

The aspect of phenomenology that is most seriously in question is the nature of the loading functions for foam-backed

panels. This subject should receive additional experimental and theoretical study.

Additional experimentally derived design data points should be obtained for the 7075-T6 and 2027-T3 panels examined here. Special attention should be directed to intermediate panel thicknesses and projectile sizes. Panels with stronger stiffeners than those used here should be tested.

The Finite element representation of hydrodynamic ram structural effects needs improvement. Effects of tear resistant bladders, impact obliquity, and projectile density should be examined.

Actual fuel tanks are likely to be struck by more than one fragment. Therefore, the single fragment work reported here should be quantitatively related to multiple-fragment effects.

APPENDIX A  
EXPERIMENTAL DETAILS

## A.1 BALLISTIC RANGE

The projectiles were launched with a 2-m-long, 20-mm-diameter launch tube. The powder was loaded into 12-gauge primed shot gun cases that were percussively detonated with a solenoid actuator. The ratio of launch energy to powder was approximately 1.2 kJ/g. Hercules 2400 was used for most shots; below 1000 m/s launch velocity, more reproducible results were obtained with Unique powder.

The cubes used in these tests were machined from 1020 steel. The hardness was Rockwell B72. The spheres were annealed ball bearings [52100 chrome manganese steel at a microhardness of 280 HV (100 g load)].

The projectiles were carried by glass-fiber reinforced Lexan<sup>®</sup> sabots. Figure A.1 illustrates a launch package. The projectiles failed at launch velocities between 1.95 km/s and 2.0 km/s. We believe the failure to have been caused by manufacturing defects. These sabots were injection molded, and inadequate heating of the mold resulted in some moisture collection in the center of each quadrant. Unfortunately, the problem was not diagnosed in time to have another batch of sabots fabricated.

The reported projectile velocities were computed from distances traveled between two radiographs. The radiographs were separated by about 46 cm; the second station was 80 cm from the target panel. The accuracy of the velocity determinations was about 0.8 percent, or about 12 m/s, typically. The projectile experienced a slight deceleration between the second radiograph and the target. Using an estimated drag coefficient for blunt cylinders of  $1.67^{(1)}$ , the slowdown was calculated to be  $0.96 \pm 0.01$  percent. This correction was ignored in the data tabulations.

Environmental data were also recorded for each shot. These included water temperature, room temperature, and barometric pressure. These data have not been reported here because no correlation with experimental results was detected.

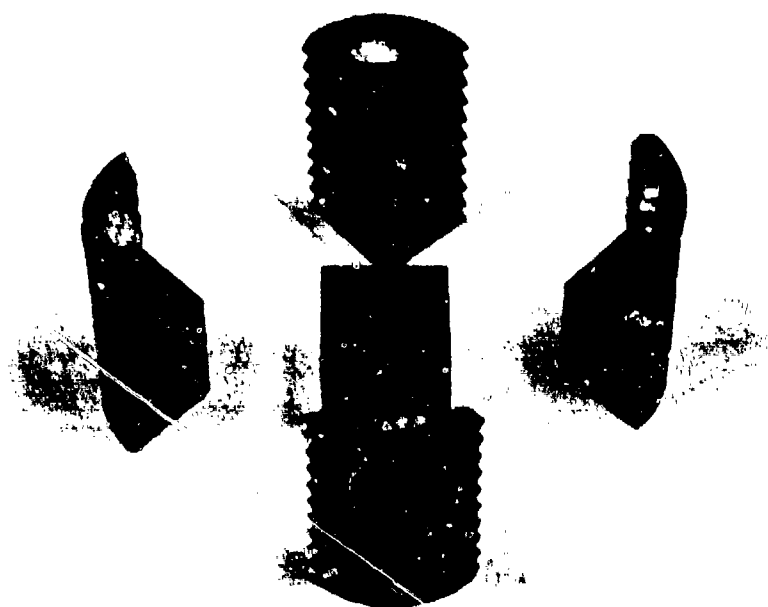


Figure A.1. Four-piece sabot and cubical projectile.

#### A.2 MOIRÉ FRINGE DATA REDUCTION

The moiré optical apparatus consisted primarily of two Ronchi rulings, commercially available lenses, a high-intensity light source, and a high-speed framing camera. The apparatus is shown schematically in Figure A.2. The mounting of the optical elements was changed from that described previously<sup>(4)</sup> to make the elements more rigid.

The light source was a "Press 50" flash lamp with an intensity of more than 0.8 Mlm for at least 30 ms. This was sufficient light for recording approximately 180 frames of information at a framing rate of 6,000 f/s. The Ronchi rulings were glass slides with evenly spaced, finely-ruled lines separated by a distance equal to the line width. The rulings had 78.7 lines per centimeter (200 lines per inch). A Fastax high-speed framing camera was used to record the moiré fringes.

There are two approaches to the analysis of data from the moiré apparatus. The most direct way is to take a picture of the fringe produced on a reference structure. The resulting



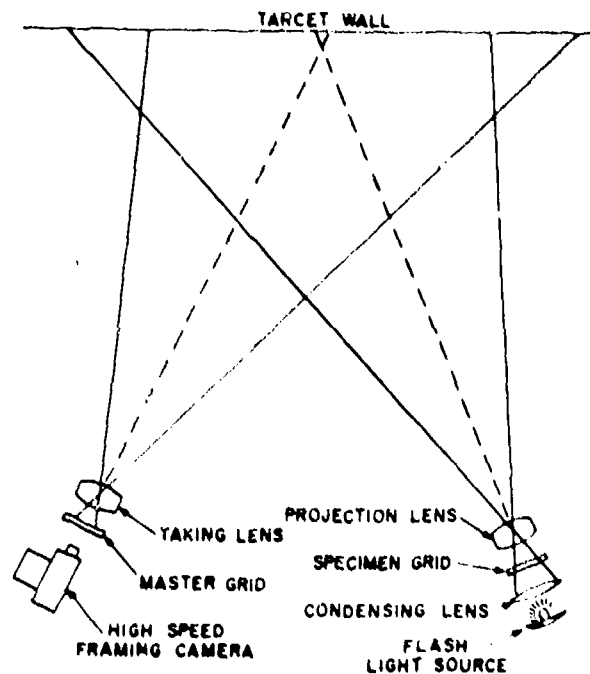


Figure A.2. Optical diagram of moiré apparatus.

"map" of fringe location can be used to deduce panel displacement. This technique was used in Reference 2.

In Reference 2, we also developed an analytical model for the reduction of fringe data. That model was so complicated that we did not find it useful. In this program the apparatus was rearranged so that the moiré triangle defined in Reference 2 became isosceles. The analysis consequently became much simpler. A computer program was written and used for reducing all the displacement data in the report except Shot FTC2.

Figure A.3 illustrates the geometry and defines the geometric variables in the present moiré triangle. The equations of the principal lines  $S_R P_O$  and  $S_L P_O$  are

$$S_R P_O \quad y = \frac{R}{S} x \quad (A1)$$

$$S_L P_O \quad y = -\frac{R}{S} x \quad (A2)$$

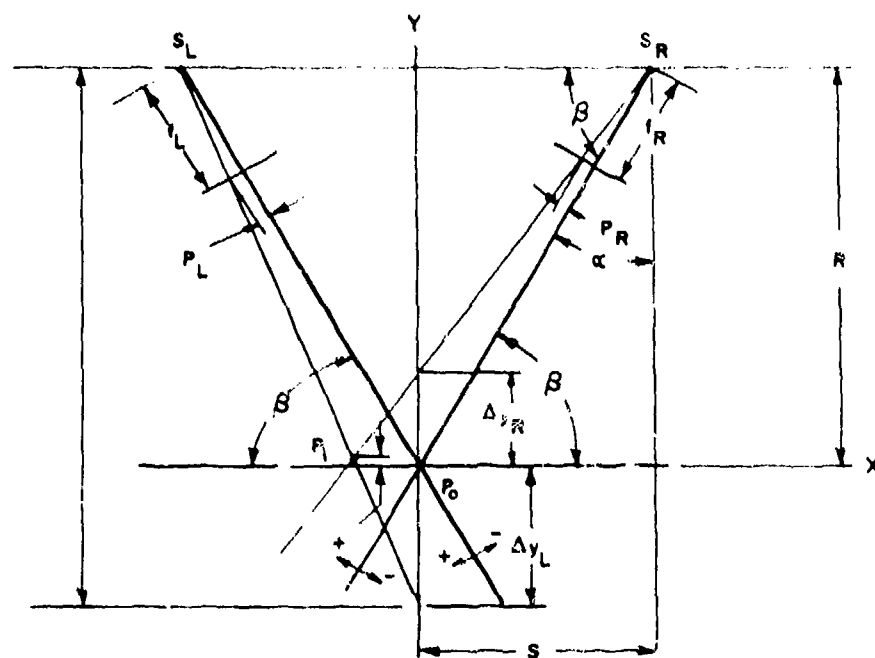


Figure A3. New moiré triangle and definition of symbols.  $S_L$  and  $S_R$  are the source and convergence points, respectively. The system is set up so that point  $P_0$  is on the target.

The equation of any other line from  $S_R$  is

$$y = m_R x + b_R \quad (A3)$$

$$= \frac{\cot \alpha + P_R/f_R}{1 - (P_R/f_R) \cot \alpha} x + R - S \left( \frac{\cot \alpha + P_R/f_R}{1 - (P_R/f_R) \cot \alpha} \right) \quad (A4)$$

The equation of any other line from  $S_L$  is

$$y = m_L x + b_L \quad (A5)$$

$$= \frac{\cot \alpha + P_L/f_L}{P_L/f_L \cot \alpha - 1} x + R + S \left( \frac{\cot \alpha + P_L/f_L}{P_L/f_L \cot \alpha - 1} \right) \quad (A6)$$

Various fringes were defined by the intersections of Equations A3 and A5. We designated the fringe through  $P_0$  as "fringe 0". Thereafter, fringe  $N$  was defined by  $i_R - i_L$ , where  $i_R$  and  $i_L$  are, respectively, integers equal to  $f_R$  and  $f_L$  divided by the Ronchi ruling pitch  $p$ . Note that fringes for which  $N > 0$  always lie above the  $x$  axis. The  $x$  and  $y$  coordinates of the fringe are given by

$$x_i = \frac{b_L - b_R}{m_R - m_L} \quad (A7)$$

$$y_i = m_R x_i + b_R \quad (A8)$$

Given  $N$ ,  $\beta$ ,  $R$ ,  $f_R$ ,  $f_L$ , and  $p$ , Equations A7 and A8 can be used to generate points on the fringes. These points were stored in a table and accessed by fringe number. Experimental data consisted of  $(x_i, N)$  pairs, where  $x_i$  was the position of fringe

N on the target surface. The program interpolated in the table to find the corresponding  $y_1$  values. Absolute values and values relative to the pre-impact panel shape were tabulated. The program also translated the x coordinates to origins centered on the tank center and on the impact point.

### A.3. PRESSURE TRANSDUCERS

Pressure pulses produced in the water by impact were measured by piezoelectric gauges. Previously (Reference 2), we had used PCB Model 101A04 quartz transducers. These were mounted in aluminum caps threaded onto 20-mm-diameter galvanized steel water pipes and inserted into the tank from the top. (The tips of the transducers protruded slightly from the caps to eliminate the possibility of air bubble formation). The closest of the transducers was about 30-cm away from the impact point. These transducers performed well for frequencies less than about 20 kHz.

The PCB gauges allowed us to compare experimental and predicted pressure histories deep within the tank. Agreement was satisfactory. However, the most difficult and important aspect of the pressure prediction was to account for the influence of the tank boundaries in the vicinity of the impact site. Similar gauges were used in this program for the  $T_4$  and  $T_5$  transducer stations. However, for the  $T_1$ ,  $T_2$ , and  $T_3$  stations there were several reasons why the quartz transducer assemblies were considered inadequate. Frequency components approaching one megahertz were anticipated in this region, and these could not be resolved with the PCB gauges. We wanted to have the sensing element as near to the panel as possible, and the bulky size of the quartz gauges made this awkward. Finally, failure of the target panel was known to be very sensitive to the pressure and flow in this region; and emplacement of a large gauge and support structure would alter these conditions.

High-frequency response is best achieved by minimizing the size of the sensing crystal and its mechanical support. Since the speed of sound in these materials is typically 6-mm/ $\mu$ s, dimensions of  $\sim 3$  mm or less would assure good frequency response up to one megahertz. This is only possible if the crystal is suspended in the water.

If the crystal is suspended in the water, it will be subjected to hydrostatic stress. This is not the usual application for piezoelectric crystals. The relevant piezoelectric equation is

$$P_i = d_i P \quad (A9)$$

where  $P$  is hydrostatic pressure,  $p_i$  is the polarization vector, and  $d_i$  is the piezoelectric coefficients. Unlike the usual piezoelectric coefficients,  $d_{ijk}$ , that comprise a third rank tensor, the  $d_i$  in Equation A9 comprises a vector (since it relates a vector to a scalar). Elementary symmetry considerations show that  $d_i = 0$  for any lattice with a center of symmetry. This includes quartz as well as the amorphous piezoelectric materials. In fact, the only common piezoelectric material for which  $d_i \neq 0$  is tourmaline, whose point group is 3-m (trigonal). Further consideration of crystal symmetry shows that  $d_1 = d_2 = 0$  for this point group, so  $d_i$  is in the direction of the triad axis<sup>5</sup>. The handbook value<sup>6</sup> of  $d_3$  for tourmaline is  $-2.16 \times 10^{-12}$  coulomb/newton.

We were only able to locate one domestic source for transducer quality tourmaline crystals: Susquehanna Instruments in Havre de Grace, Maryland. This company assembled the electrical components to our specifications and calibrated the gauges.

The gauge design is shown in Figure A4. The sensing element was a tiny tourmaline crystal, typically 1-mm-thick. Twin electrical leads 0.2-mm in diameter were attached to the

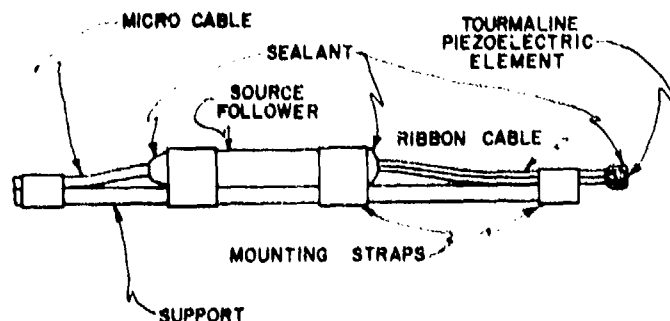


Figure A.4. Schematic drawing of gauge design.

crystal with conducting epoxy. This area was sealed with insulating epoxy. The twin leads ran 40-mm, where they entered a piece of heat-shrinkable tubing. The opening was sealed with an RTV compound. After another 30-mm, the leads were attached to a commercial voltage follower (PCB Model 401M26). The unit converted the high impedance crystal voltage to 100 $\Omega$  output impedance. The rear of the amplifier was connected to a coaxial cable. The shrink tubing closed and was sealed onto this cable. The cable conducted the signal out of the water to a recording oscilloscope. The main body of the gauge was attached to a 6-mm copper tube; but the crystal and 40-mm-long twin cable were free in the water, facing the impact site. Calibration factors ranged from 0.96 bar/mV to 1.29 bar/mV.

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